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JANUARY 1953

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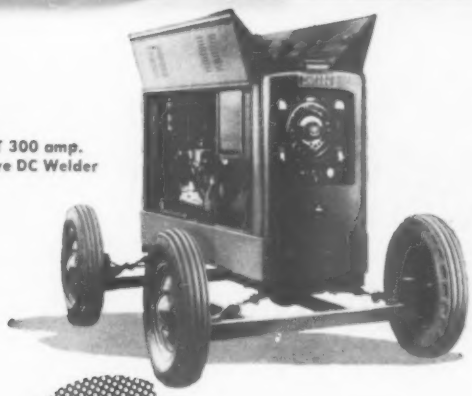

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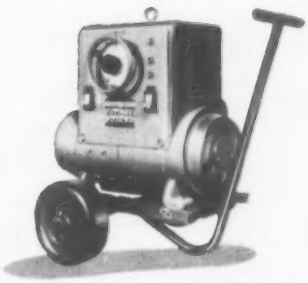

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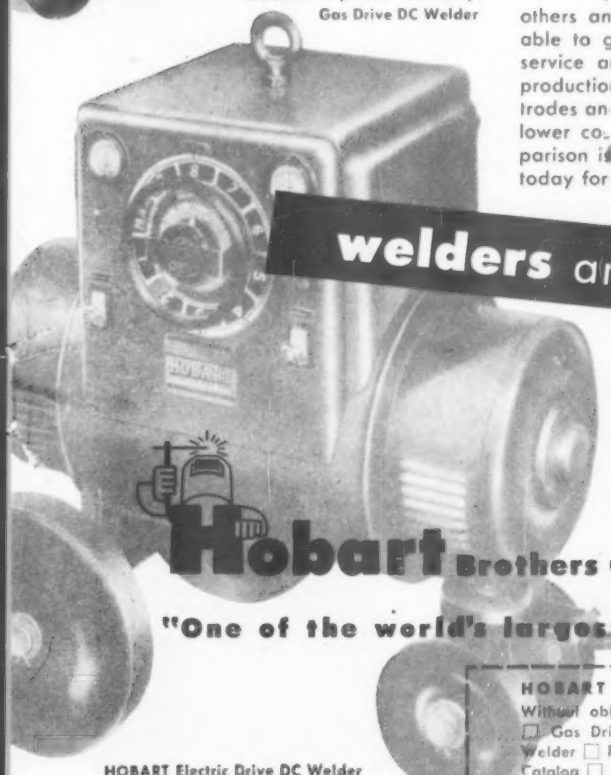

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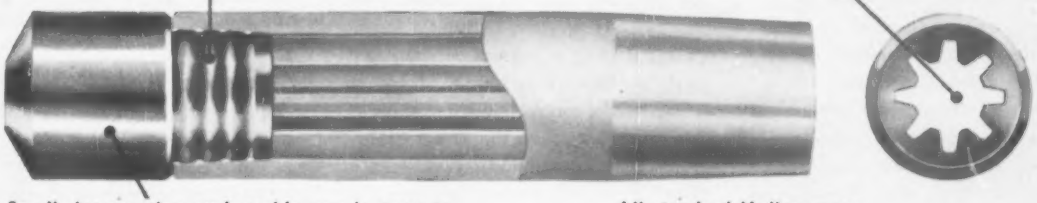

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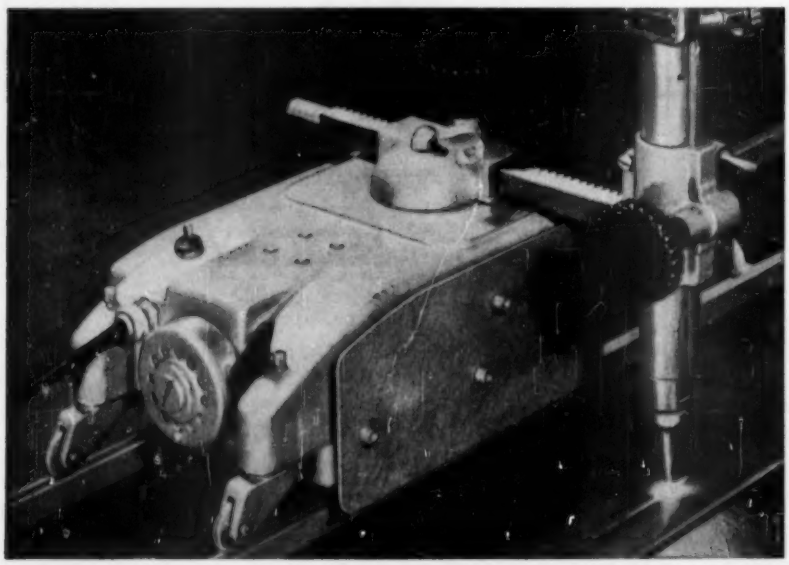

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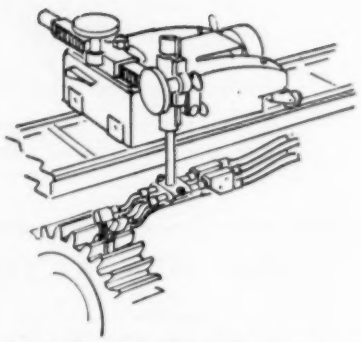

Fig. 2—Flame-treating work requires precise control of applied heat. The CM-45, with suitable apparatus, assures you of efficiency and uniformity on every job.

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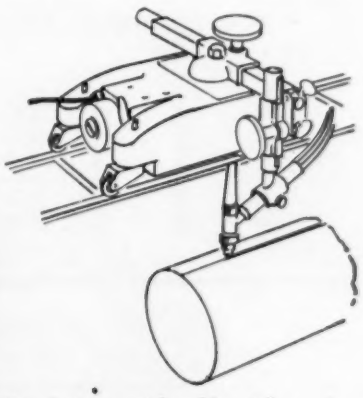

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Editor
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The WELDING JOURNAL

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Spot Welding of Magnesium with Three-Phase Low Frequency Equipment

Satisfactory spot welds in magnesium can be produced with low frequency converter machines by using single or dual force system and with or without postheat

by Paul Klain, D. L. Knight and J. P. Thorne

INTRODUCTION

THE purpose of this investigation was to determine the usefulness of the low frequency converter equipment for spot welding magnesium. The investigation was limited to a determination of the effects of machine and welding control variables upon the quality of magnesium alloy spot welds.

Magnesium alloys can be spot welded to Mil-W-6860 specifications on stored energy, direct energy single-phase and direct energy three-phase dry disk rectifier machines. Most of the available information on the spot welding of magnesium alloys was obtained prior to the close of World War II and on the stored energy or direct energy single-phase a-c type of machine. Activity in the welding of light metals was greatly reduced following the war and manufacture of stored energy equipment was almost entirely discontinued. In the meantime, three-phase low frequency direct energy equipment was developed to reduce power demand over the single-phase equipment. Due to its greater flexibility in being able to weld all types of metals, this equipment has become very popular in the present emergency for the welding of light metals. Most of the newer machines for use on light metals are being equipped with dual force system (forge) and postweld heat. Neither of these variables has been used much with magnesium and very little information is available as to their effects. Therefore, an evaluation of these variables was included in this investigation.

The dual force cycle (forge) is one in which the weld is made at a low force followed by the application of a higher force to squeeze or forge the solidifying weld. The correct timing of the application of the forge force is extremely important since its purpose is to reduce internal defects. If the forging force is applied too late, the weld will have solidified and welds with internal defects will have resulted. If the forging force is applied too soon, the welding current will be insufficient with the higher force and weaker welds will result.

Postheat is often used on air-hardening steels to prevent hardening or cracking due to too rapid cooling of the welds. This variable is controlled independently of the welding current and is usually some fraction of the total welding current. The duration of the postheat can be varied over a wide range within the "hold time" portion of the weld cycle.

The frequency converter welders use special transformers and controls which take power at line frequencies and transform it into a series of unidirectional pulses of current in the secondary or welding circuit. The secondary current pulse is characterized by a slowly rising wave front. The most commonly used frequencies are 20, 12, $8^4/_7$, $6^2/_3$, and $5^5/_{11}$ cycles. The controls may allow the use of full cycle or half cycle

Paul Klain is connected with the Dow Chemical Co. and **D. L. Knight** and **J. P. Thorne** are connected with the National Electric Welding Machines Co.

Presented at the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion March 15, 1953.

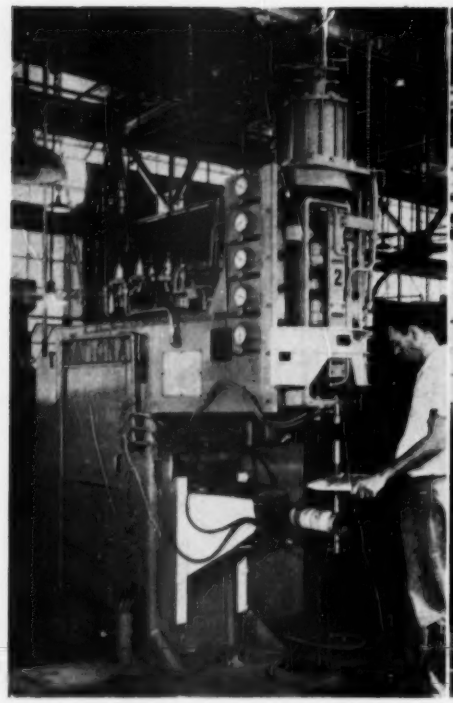


Fig. 1A The 200-kva three-phase machine in operation on a magnesium assembly

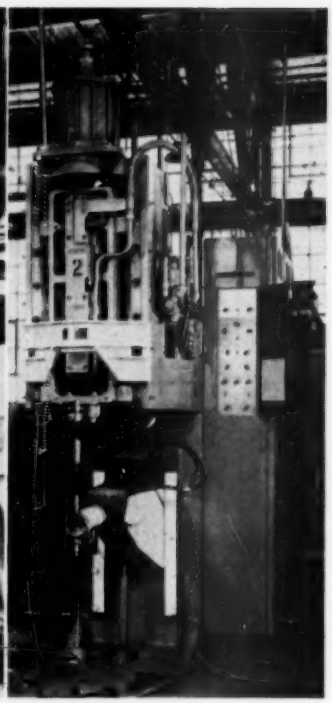


Fig. 1B View of the 200-kva three-phase machine showing the control panel

weld time. The duration of the flow of the unidirectional secondary current is known as the pulse time and is controlled by rectification of the three phases at a constant polarity one or more times in succession. When the pulses are of alternating polarity, a non-sinusoidal alternating current is produced. Thus, a single pulse of alternating polarity produces a current of 20-cycle frequency while a three-pulse results in a current of $8^4/_7$-cycle frequency.

The spot welder used in this investigation was rated at 200 kva three phase with a 36-in. throat and is shown in Figs. 1*A* and 1*B*. It was equipped with a roller bearing ram and frictionless cylinders for accurate repetitive pressures and fast follow-up of the welding electrode. The maximum welding force available was 9000 lb and the machine capacity was a minimum thickness of two pieces of 0.025-in and a maximum thickness of two pieces of 0.250-in. magnesium. The welding control was the standard type of three-phase frequency converter control in use for spot welding to meet Mil-W-6860 specifications with the following adjustable heat and timing functions: squeeze time, weld time, hold time, off time, postheat time, forge delay time, weld current and postheat current. A wave pattern of full cycle, one-half cycle alternate and unipolarity was available.

The scope of the investigation was limited to the determination of the effects of welding force, forge force, postheat current and time, and frequency (weld time) on the quality of the welds. For determination of the effects of these variables, the weld time and tip radii were held constant. Values which were based on past research, and which were felt to be optimum for this gage, were used. One-half-cycle, alternate polarity firing was used with a pulse time of $3^1/_3$ cycles 60-cycle basis (frequency of $8^4/_7$ cps) and a tip radius of 4 in. for both upper and lower electrodes. Preliminary tests had indicated no significant difference existed between the half and full cycle weld time.

PROCEDURE

The stock used in all tests was 0.064-in. AZ31A annealed sheet (Dow FS1-0) having the nominal composition 3Al – 1Zn – 0.2Mn – balance Mg. The material was treated in Dow Bright Pickle (Chemical Treatment No. 15—Chromic Acid (CrO_3), 24 oz; Sodium Nitrate ($NaNO_3$), 4 oz; and water to make 1 gal) and then wire brushed a few minutes before spot welding. The welding tests were carried out on test pieces consisting of two sheets $10^1/_2$ x 5 in. with an overlap of $1^1/_2$ in. Each test piece contained seven welds spaced $1^1/_2$ in. apart. The seven welds were radiographed on Eastman Type A film. The first five welds were cut into $1^1/_2$-in. wide test bars for single spot shear strength tests. The remaining two welds were sectioned for microscopic examination and for determination of weld diameter, penetration and soundness. All tests performed on these welds were carried out as specified in Mil-W-6860.

The electrode tips were $1^1/_4$ in. in diameter with 4-in. radius domes, RWMA Group A, Class 2 type. The tips were cleaned with No. 320 grit aloxite cloth before welding any of the seven-weld test pieces. A strain gage was mounted on the upper electrode holder for checking the magnitude and duration of the applied force. These values were recorded on a Brush Oscillograph. The magnitude and the time of application of the force were easily determined from the oscillographic traces. The welding and postheat current values were also obtained from the oscillograph traces on a second channel.

In order to properly evaluate the variables, the effects of welding force alone on the spot-weld characteristics had to be established first. This was done by selecting at least four values of current from no-weld to expulsion for a given welding force and determining the resulting strength-current characteristics. This allowed the selection of welding conditions as bases upon which the effects of the variables, forge and postheat, could be evaluated.

The weld symmetry, penetration and soundness were

determined on the sectioned and polished welds, which were the last two welds in the series of seven in each test piece. For low power examination, the specimens were etched in 5% acetic acid solution for 5 sec. This was an excellent method for revealing cracks and for outlining the weld nugget. For higher magnification, the glycol etchant[4] was better because of its slower etching rate. The eutectic stringers in the heat-affected zone were easily revealed by this etchant.

RESULTS

Welding Force

In Table I are given the complete data for spot welds in 0.064-in. AZ31A-0 sheet. The welds were made at various electrode forces with increasing currents from no-weld to expulsion. Welding forces from 1180 to 3400 lb were tried. Included in the data are the welding current, average single spot strength,

Table 1—Effect of Electrode Force on Spot Welds in 0.064-In. AZ31A-0 Sheet Made on the Three-Phase Low Frequency Converter System at a Frequency of 8 1/7 Cycles on 1/2 Cycle, Alternate Polarity

Electrodes 1 1/4 in. diameter, 4-in. R domes, wire-brushed sheet

Electrode force, lb	*Rms welding current, amp*	*Avg spot strength, lb**	*Avg spot diameter, in.**	*% Penetration*	*Radiography*	*Type of failure†*
1180	29,100	508	0.209	40–45	Cracked	S
1180	32,200	718	0.270	60–65	Cracked	S
1180	35,200	860	0.295	75–80	Cracked	T + S
1180	37,900	987	0.321	65–70	Cracked and expulsion	T + S
1380	29,100	354	0.170	25–40	Sound	S
1380	32,200	613	0.238	40–50	Cracked	S
1380	37,900	910	0.298	65–70	Cracked	T
1380	39,900	1008	0.330	70–80	Cracked and expulsion	T + S
1750	32,200	312	0.164	15–20	Sound	S
1750	35,200	581	0.223	30–45	Sound	S
1750	39,900	866	0.300	45–50	Cracked	T
1750	46,200	1023	0.345	65	Cracked	T
1750	48,500	1058	0.356	85	Cracked	T
1750	50,800	1097	0.364	70	Cracked and expulsion	T + S
2000	37,900	719	0.253	30–45	Sound	S
2000	39,900	823	0.289	50–55	Sound	T
2000	46,200	940	0.331	65	Cracked	T
2000	50,800	1104	0.366	75–85	Cracked	T
2000	53,200	1145	0.375	75–80	Cracked	T
2000	54,900	1192	0.397	80	Cracked and expulsion	T + S
2340	39,900	640	0.244	20–30	Sound	S
2340	43,600	833	0.283	35–40	Sound	S
2340	48,500	941	0.336	45–60	Sound	T
2340	54,900	1069	0.383	65–70	Cracked	T
2340	58,600	1155	0.398	80–90	Cracked and expulsion	T + S
2550	43,600	668	0.236	20–25	Sound	S
2550	48,500	896	0.305	35–40	Sound	T
2550	53,200	996	0.345	65	Sound	T
2550	56,700	1075	0.379	55–65	Sound	T
2550	60,000	1103	0.395	65–80	Cracked	T
2550	61,200	1107	0.405	75–85	Cracked	T
2550	62,600	1217	0.413	80–95	Cracked and expulsion	T
2650	48,500	878	0.313	30–50	Sound	S
2650	53,200	1033	0.352	50	Sound	T
2650	58,600	1078	0.373	55–65	Sound	T
2650	62,600	1096	0.408	70–80	Cracked	T
2650	65,200	1296	0.413	55–80	Cracked and expulsion	T
2860	50,800	901	0.311	25–40	Sound	T
2860	54,900	1010	0.355	45	Sound	T
2860	61,200	1071	0.386	55–70	Sound	T
2860	61,200	1132	0.388	55–65	Sound	T
2860	65,200	1166	0.420	65	Sound	T
2860	66,400	1258	0.423	70–75	Cracked and expulsion	T
3400	53,200	790	0.278	15–20	Sound	T
3400	56,300	1008	0.328	20–40	Sound	T
3400	61,200	1059	0.369	45–55	Sound	T
3400	66,400	1226	0.428	65–80	Sound	T
3400	67,900	1283	0.431	70–80	Sound	T
3400	68,700	1320	0.434	70–80	Cracked and expulsion	T

* Average of 5 welds.
† T = Tear of parent metal around spot. S = Shear through spot.

Table 2—Summary of Welding Conditions for Sound Spots in 0.064-In. AZ31A-0 Sheet with the Three-Phase Low Frequency Converter Machine at $8^4/_7$ Cycles

Electrodes $1^1/_4$ in. diameter, 4-in. R dome tips, wire-brushed sheet

Electrode force, lb	*Range of weld strength, lb*	*Range of spot diameters, in.*	*Range of welding currents, amp*	*% Penetration*	*Limiting conditions*
1750	581	0.223	35,200	30–45	Cracks and expulsion
2000	719–823	0.253–0.289	37,900–39,900	30–55	Cracks and expulsion
2340	833–941	0.283–0.336	43,600–48,500	35–60	Cracks and expulsion
2550	896–1075	0.305–0.379	48,500–56,700	35–65	Cracks and expulsion
2650	878–1078	0.313–0.373	48,500–58,600	30–65	Cracks and expulsion
2860	901–1166	0.311–0.420	50,800–65,200	25–65	Cracks and expulsion
3400	1008–1283	0.328–0.431	53,200–67,900	20–80	Cracks and expulsion

average spot diameter, percent penetration and soundness. The electrode forces were determined from strain gage traces on a Brush Oscillograph.

At electrode forces below 1750 lb all welds were cracked. At higher forces, the cracking decreased and more sound spots were obtained. However, as the current increased for any given force, some cracking below the expulsion limit also occurred. The graph of Fig. 2 shows strength-current characteristics as determined at 2000-, 2340-, 2550- and 3400-lb forces. All of the curves have the same general shape. The range in currents over which sound crack-free welds were obtained increased with increased electrode forces. This is shown more clearly in Table 2, by the summary of conditions for sound spot welds. These values were plotted in Fig. 3 as weld strength against electrode force and in Fig. 4 as electrode force against the approximate rms welding current. The graphs showed an expanding range of spot strengths and secondary currents with increasing electrode force. The ranges of secondary currents and of strengths for sound welds at any force less than about 2500 lb were quite narrow and were too critical for practical use. Therefore, for the 0.064-in. sheet and for single force, $^1/_2$-cycle welds at $8^4/_7$ frequency, the optimum welding conditions appeared to be those existing at an electrode force of 2500 lb.

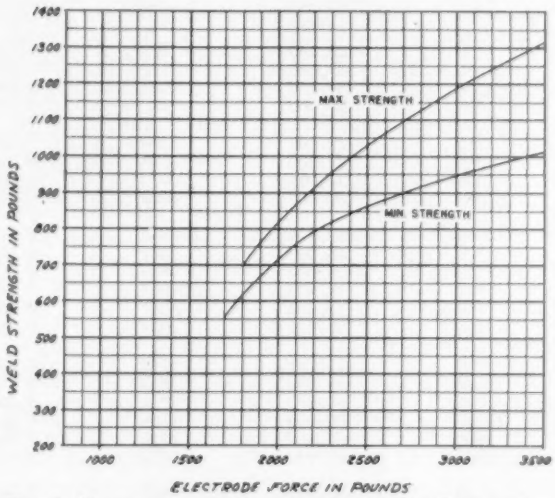


Fig. 3 Range of sound spot welds obtained at various electrode forces. Welds in 0.064-in. AZ31A-0 sheet at $^1/_2$-cycle, alternate polarity, frequency of $8^4/_7$ cycles

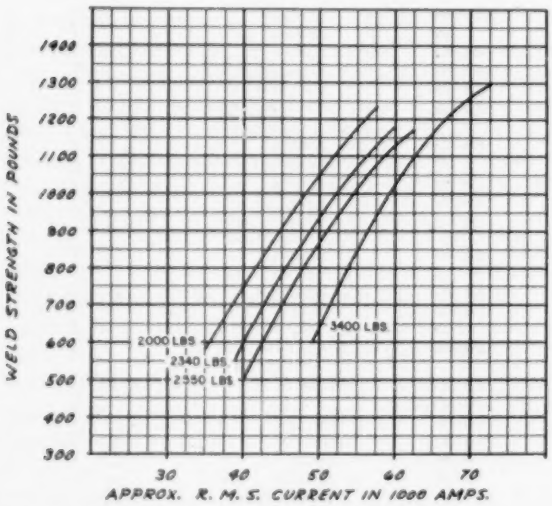


Fig. 2 Strength-current characteristics with optimum electrode forces for 0.064-in. AZ31A-0 sheet. One-half-cycle, alternate polarity welds at $8^4/_7$-cycle frequency

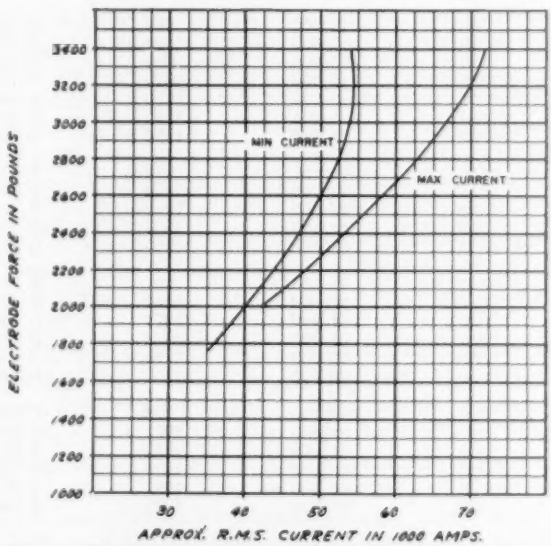


Fig. 4 Electrode force-current relationship for sound welds in 0.064-in. AZ31A-0 sheet. One-half-cycle, alternate polarity welds at $8^4/_7$-cycle frequency

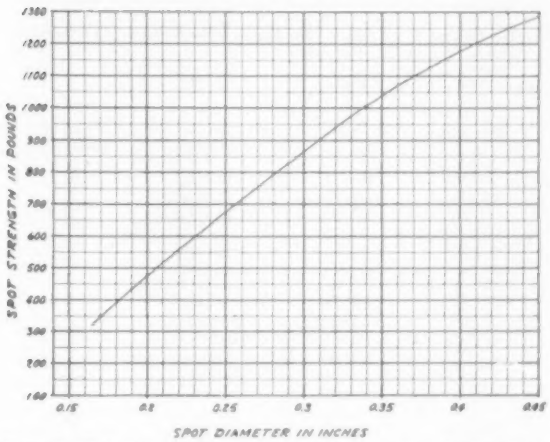


Fig. 5 Relation of spot strength to spot diameter in 0.064-in. AZ31A-0 sheet welds made on frequency converter machine. Single force welds. One-half-cycle, alternate polarity, at frequency of 8 1/7 cycles. (Average of 5 tests)

Each of the strength and spot diameter values listed in Table 1 were plotted in Fig. 5 to show the relationship between spot strength and spot diameter. Many of the spots were cracked, but the points fell very closely to the curve shown. Examination of tested specimens revealed that failure occurred as either shear of the nugget or ductile tearing of the sheet. The shear strengths were not affected by the presence of cracks in the spots. Based on the observation of 20 to 80% penetration, the shear values ranged from 650 to 1200 lb per spot corresponding to 0.24 to 0.41 in. spot diameter.

Forge Force

It has been shown that crack-free welds in 0.064-in. AZ31A-O sheet with the single force system could be obtained at electrode forces of 2000 lb or higher but that at 2500 lb the range of sound spots was sufficiently broad to indicate optimum conditions prevailed. In order to determine the effect of forge force on spot characteristics, the welding forces were selected at half the optimum force, or in the range of 1200–1300 lb.

The results of application of forge force in ratios of 1:1 1/2, 1:2 and 1:3 are given in Table 3. Welding forces of 1200 and 1300 lb were used. At 1300 lb weld and 1920 lb forge force (1:1 1/2), all the welds were cracked regardless of the point of application of forge force with respect to the weld current. When the ratio of weld to forge force was increased to 1:2, sound welds were obtained at forge delays of 3.3 and 3.6 cycles but cracked welds and expulsion were obtained at forge delays of 4.6 cycles, indicating that forge was applied too late. As the ratio was increased to 1:3, sound but considerably smaller spots were obtained at the same weld current. Again, cracked spots were obtained only at the 4.6-cycle forge delay time indicating that forge delays greater than 3.6 cycles were too long. Selection of a higher initial welding force of 1600 lb, resulted in sound, crack-free welds at a force ratio of 1:1 1/2. The point of application of forge force was less critical, since even at forge delay of 4.6 cycles, sound welds were obtained. It was evident that the dual force allowed a lower initial force to be used with lower power demand for sound welds than the single force system. The ratio of weld to forge force required to produce sound welds depended on the level of the initial welding force selected. At 1300 lb weld force, a 1:2 ratio was required for sound welds, whereas at 1600 a 1:1 1/2 ratio was sufficient and the application of forge force was less critical. Comparison of welds with and without forge showed no differences in strengths. A comparison of the currents required showed 48,600

Table 3—Effect of Forge Force on Spot Welds in 0.064-In. AZ31A-0 Sheet Welded on the Three-Phase Low Frequency Converter Machine at 8 4/7 Cycles on 1/2 Cycle Alternate Polarity

Electrodes 1 1/4 in. diameter, 4-in. R domes, wire-brushed sheet

Electrode force, lb		Approx. ratio, weld to forge force	Forge delay cycles*	Avg weld current, amp	Avg spot strength, lb†	Avg spot diameter, in.†	% Penetration	Radiography	Type of failure‡
Weld	Forge								
1300	1920	1:1 1/2	3.3	43,600	872	0.298	65–75	Cracked	T
1300	1920	1:1 1/2	3.6	43,600	912	0.309	65–70	Cracked	S + T
1300	1920	1:1 1/2	4.6	43,600	1073	0.336	75–80	Cracked and expulsion	S
1300	2750	1:2	3.3	43,600	870	0.295	55–70	Sound	S
1300	2750	1:2	3.6	43,600	871	0.297	65–70	Sound	T + S
1300	2750	1:2	4.6	43,600	1049	0.334	75–80	Cracked and expulsion	S
1200	3900	1:3 1/4	3.3	43,600	691	0.253	40–50	Sound	S
1200	3900	1:3 1/4	3.6	43,600	651	0.233	30–50	Sound	S
1200	3900	1:3 1/4	4.6	43,600	1032	0.328	70–80	Cracked	S
1600	2500	1:1 1/2	3.3	43,600	667	0.238	30–45	Sound	S
1600	2500	1:1 1/2	3.3	46,200	792	0.277	40–55	Sound	T + S
1600	2500	1:1 1/2	3.6	46,200	827	0.291	45–60	Sound	T + S
1600	2500	1:1 1/2	4.6	46,200	1011	0.331	70–75	Sound	T

* 60 cycle base.
† Average of 5 welds.
‡ T = Tear of parent metal around spot. S = Shear through spot.

Table 4—Effect of Postheat on Spot Characteristics in 0.064-In. AZ31A-0 Sheet Welded on the Three-Phase Low Frequency Converted Machine at 8 4/7 Cycles on 1/2 Cycle Alternate Polarity

Electrodes 1 1/4 in. diameter, 4-in. R domes, wire-brushed sheet

Electrode force, lb	Welding current, amp	Postheat		Avg spot strength, lb*	Avg spot diameter, in.*	% Penetration	Radiography	Type of failure†
		Time, cycles	Current, amp					
1700	37,900	1	21,600	707	0.298	65–75	Cracked	T
1700	37,900	2	21,600	803	0.294	65–70	Cracked	T
1700	37,900	3	21,600	724	0.275	55–60	Cracked	T
1700	37,900	4	21,600	763	0.291	55–70	Cracked	T
1700	37,900	5	21,600	707	0.286	55–65	Cracked	T
1700	37,900	6	21,600	751	0.285	60–65	Cracked	T
1700	37,900	1	26,550	777	0.287	50–70	Cracked	T
1700	37,900	2	26,550	773	0.280	55–60	Sound	T
1700	37,900	3	26,550	741	0.285	50–65	Sound	T
1700	37,900	4	26,550	732	0.278	45–60	Sound	T
1700	37,900	5	26,550	699	0.285	60	Sound	T
1700	37,900	6	26,550	709	0.287	60	Sound	T
1800	43,600	1	24,800	849	0.336	70–85	Cracked	T
1800	43,600	2	24,800	891	0.336	70–80	Cracked	T
1800	43,600	3	24,800	853	0.323	75–80	Cracked	T
1800	43,600	4	24,800	883	0.330	70	Cracked	T
1800	43,600	5	24,800	876	0.334	70–80	Cracked	T
1800	43,600	6	24,800	979	0.333	65–80	Cracked	T
1800	43,600	1	30,600	917	0.331	75–85	Cracked	T
1800	43,600	2	30,600	919	0.337	65–80	Cracked	T
1800	43,600	3	30,600	942	0.322	75–80	Sound	T
1800	53,600	4	30,600	925	0.334	65–70	Sound	T
1800	43,600	5	30,600	881	0.326	60–80	Sound	T
1800	43,600	6	30,600	907	0.336	70–80	Sound	T
1800	43,600	None	30,600	1067	0.341	75–80	Cracked	T

* Average of 5 welds.
† T = Tear of parent metal around spot. S = Shear through spot.

amp for the single force and 43,600 amp for the dual force welds of the same size.

Postheat

The data for welds made with single force and postheat are given in Table 4. At an electrode force of 1700 lb and a postheat current of 21,600 amp, equal to 57% of the weld current (37,900 amp), all welds were cracked but the degree of cracking was reduced. The duration of the postheat current, from 1 to 6 cycles (60 cycle basis), did not eliminate cracking in the welds. When the postheat current was increased to 70% of the weld current, cracked welds were eliminated after two or more cycles of postheat time. Additional tests were made on larger spots and slightly higher (1800 lb) electrode force. Again at 57% postheat current, cracking in welds was not eliminated but was reduced. At 70% postheat current, cracking in welds was completely eliminated after 3 cycles of postheat. Therefore, it was concluded that postheat was beneficial in reducing and eliminating cracking in welds at the lower electrode forces. Postheat may prove advantageous in certain applications requiring low electrode forces.

The effect of postheat on the strength of spots was revealed by plotting spot strengths with and without postheat against spot size. The resulting graph is shown in Fig. 6. The welds with postheat showed lower strengths by 10 to 15%. Observations indicated that the heat-affected area widened with postheat current and time but this could not be brought out too clearly.

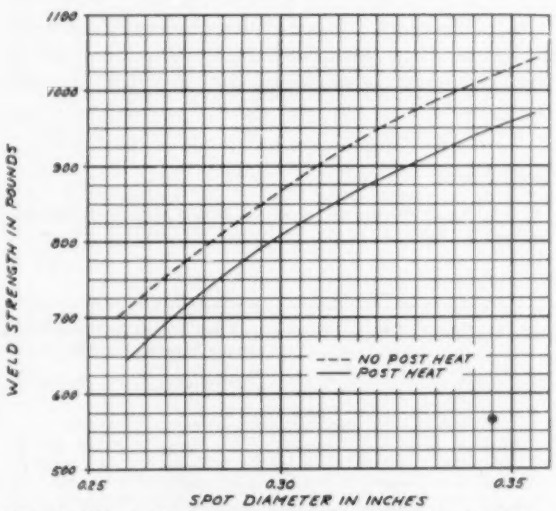


Fig. 6 Effect of postheat on weld strength of 0.064-in. AZ31A-0, wire brushed. One-half-cycle welds at 8 4/7-cycle frequency. (Each point average of 5 values)

Combination of Forge Force and Postheat

In this series of tests, the ratio of weld to forge force was held constant at 1:1 1/2. The actual values were 1200 and 1920 lb. At these values as shown by previous tests, it was not possible to obtain crack-free welds. Therefore, postheat currents equal to 57 and

Table 5—Effect of a Combination of Forge and Postheat on the Weld Characteristics in 0.064-In. AZ31A-0 Sheet; 1/2-Cycle Welds at 8 1/7 Cycles on the Frequency Converter Machine

Electrodes 1 1/4 in. diameter, 4-in. R domes, wire-brushed sheet

Electrode force, lb		Forge delay, cycles	Postheat		Rms welding current, amp	Avg spot strength, lb	Avg spot diameter, in.	% Penetration	Radiography
Weld	Forge		Time, cycles	Current, amp					
1200	1920	3.3	None	...	43,600	933	0.302	65	Cracked
1200	1920	3.3	1	24,800	43,600	895	0.306	60–80	Cracked
1200	1920	3.3	2	24,800	43,600	836	0.302	65–70	Sound
1200	1920	3.3	3	24,800	43,600	838	0.300	...	Sound
1200	1920	3.3	4	24,800	43,600	822	0.298	65–70	Sound
1200	1920	3.3	6	24,800	43,600	802	0.292	60	Sound
1200	1920	3.3	7	24,800	43,600	820	0.297	70	Sound
1200	1920	3.3	8	24,800	43,600	800	0.295	65–75	Sound
1200	1920	3.6	..	...	43,600	867	0.295	55–65	Cracked
1200	1920	3.6	1	24,800	43,600	853	0.305	60–65	Cracked
1200	1920	3.6	2	24,800	43,600	844	0.297	60–65	Cracked
1200	1920	3.6	4	24,800	43,600	818	0.292	60–65	Sound
1200	1920	3.6	6	24,800	43,600	796	0.286	60–65	Sound
1200	1920	3.6	8	24,800	43,600	784	0.284	55–65	Sound
1200	1920	4.6	..	...	...	900	0.313	70–80	Cracked
1200	1920	4.6	1	24,800	43,600	946	0.319	60–75	Cracked
1200	1920	4.6	2	24,800	43,600	959	0.322	70	Cracked
1200	1920	4.6	4	24,800	43,600	868	0.313	60–85	Sound
1200	1920	4.6	6	24,800	43,600	902	0.314	70–80	Sound
1200	1920	4.6	8	24,800	43,600	892	0.316	...	Cracked
1200	1920	3.3	1	30,600	43,600	779	0.289	55–60	Cracked
1200	1920	3.3	2	30,600	43,600	831	0.298	70	Sound
1200	1920	3.3	4	30,600	43,600	827	0.295	65	Sound
1200	1920	3.3	6	30,600	43,600	802	0.294	60–70	Sound
1200	1920	3.6	1	30,600	43,600	764	0.286	60	Cracked
1200	1920	3.6	2	30,600	43,600	829	0.291	60–70	Sound
1200	1920	3.6	4	30,600	43,600	828	0.295	65–70	Sound
1200	1920	3.6	6	30,600	43,600	828	0.305	65–70	Sound
1200	1920	4.6	1	30,600	43,600	907	0.306	70–75	Cracked
1200	1920	4.6	2	30,600	43,600	925	0.317	70	Sound
1200	1920	4.6	4	30,600	43,600	895	0.313	70–75	Sound
1200	1920	4.6	6	...	43,600	865	0.309	70	Sound

70% of the welding current were tried. The postheat time was varied from 1 to 8 cycles while the forge delay time was varied from 3.3 to 4.6 cycles.

The results of adding postheat to the welding cycle are given in Table 5. At a postheat current of 24,800 amp or 57% of the weld current and a forge delay of 3.3 cycles, a postheat time of 2 cycles was sufficient to eliminate cracking in the welds. At forge delays of 3.6 and 4.6 cycles, a postheat time of 4 cycles was required to produce sound welds. When the postheat current was increased to 70% of the weld current (30,600 amp), a postheat time of 2 cycles was sufficient to give sound welds for forge delays up to 4.6 cycles. These results showed that the additon of postheat to the dual force weld cycle could eliminate cracking in the welds. Under the conditions of these tests, the ratio of weld to forge force for sound welds was reduced from 1:2 to 1:1 1/2 with postheat. At the lower postheat current, sound welds were obtained at relatively long forge delays by holding the postheat sufficiently long to allow forging

Table 6—Characteristics of Spot Welds in 0.064-In. AZ31A-0 Sheet Made with Frequencies of 20, 12, 8 1/7, 6 2/3 and 5 5/11 Cycles on 1/2-Cycle Alternate Polarity

Electrodes 1 1/4 in. diameter, 4-in. R domes, wire-brushed sheet

Electrode force, lb		Weld current, Amp	Frequency, cycles	Postheat		Forge delay, cycles	Avg weld strength lb*	Avg spot diameter in.*	% Penetration	Radiography
Weld	Forge			Time, cycles	Current, amp					
1200	2000	70,300	20	..	...	3.3	735	0.269	65	Cracked and expulsion
1200	2000	70,300	20	4	49,400	3.3	764	0.278	65	Cracked and expulsion
1200	2000	70,300	20	4	40,200	3.3	855	0.292	65–80	Sound
1200	2000	46,200	12	..	...	3.3	746	0.267	55–60	Cracked
1200	2000	46,200	12	4	26,400	3.3	694	0.264	55–65	Sound
1200	2000	46,200	12	4	32,400	3.3	704	0.264	55–65	Sound
1200	2000	43,600	8 1/7	..	...	3.3	933	0.302	65	Cracked
1200	2000	43,600	8 1/7	4	24,800	3.3	822	0.298	65–70	Sound
1200	2000	43,600	8 1/7	4	30,500	3.3	827	0.295	65	Sound
1200	2000	39,900	6 2/3	..	...	3.3	880	0.306	65	Cracked
1200	2000	39,900	6 2/3	4	22,800	3.3	849	0.306	55–65	Sound
1200	2000	39,900	6 2/3	4	28,000	3.3	884	0.303	65–70	Sound
1200	2000	37,900	5 5/11	..	...	3.3	854	0.295	65–70	Cracked
1200	2000	37,900	5 5/11	4	21,600	3.3	770	0.295	50–60	Sound
1200	2000	37,900	5 5/11	4	26,500	3.3	856	0.306	55–65	Sound

* Average of 5 welds.

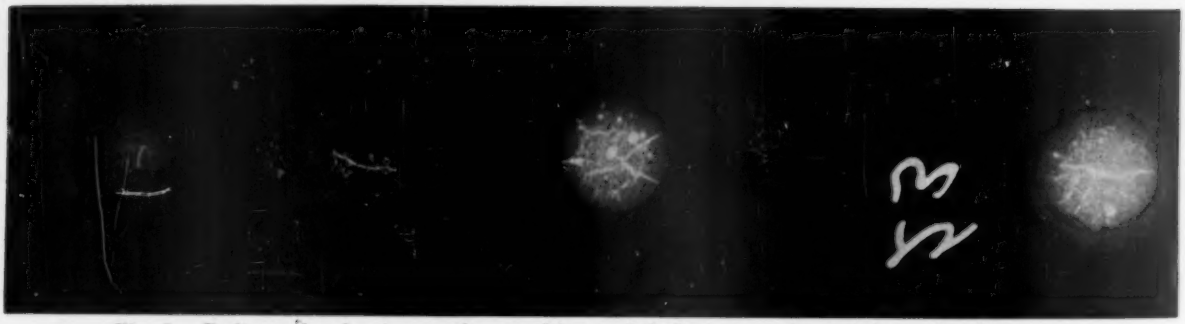


Fig. 7 Radiographs showing cracks, expulsion and dark ring, at the periphery of weld nugget

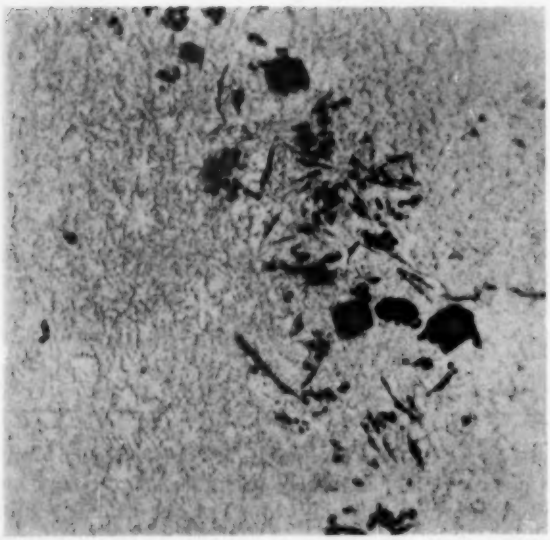


Fig. 8 Primary Mn particles segregated at the periphery of the nugget. Weld nugget to the left as shown by dendritic structure. Glycol etch. 250 ×

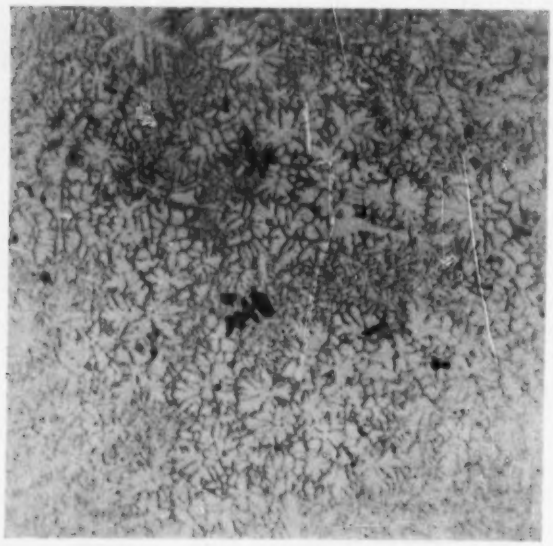


Fig. 9 Center of the weld nugget showing only occasional gray Mn particles in a dendritic structure. Glycol etch 250 ×

to take place while the weld was still in the plastic condition. At the higher postheat current, the postheat time for sound welds was shorter and less critical because the higher heat allowed the weld to remain in the plastic state sufficiently long for the forging action to become effective.

The effect of postheat on the shear strengths of these forged welds could not be definitely established because of insufficient data. There was evidence that a slight reduction in strengths was obtained. However, from the viewpoint of the single force postheat results, the dual force postheat welds had higher strengths, and therefore could be interpreted to mean that the addi-

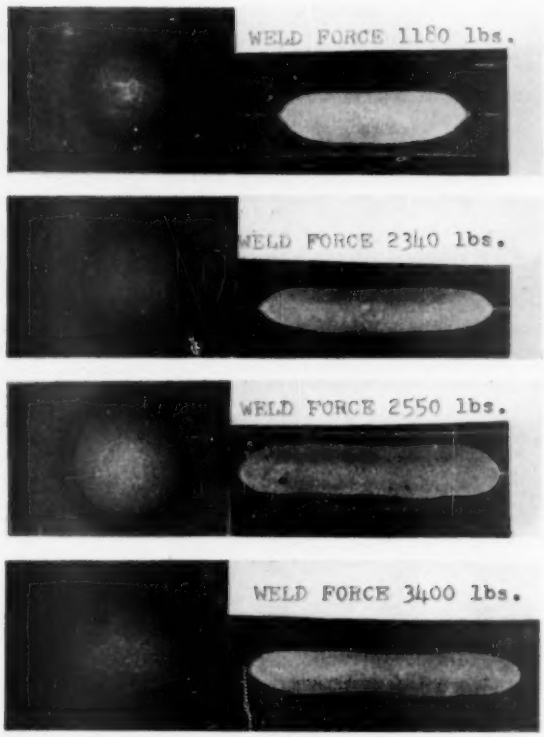


Fig. 10 Cracked and sound welds as shown by radiographs and macrographs of the same spot. Single force $\frac{1}{2}$-cycle welds in 0.064-in. AZ31A-0 sheet made at a frequency of $8\frac{1}{7}$ cycles. Radiographs, 2×; macrographs, 5×. Etchant 5% acetic acid

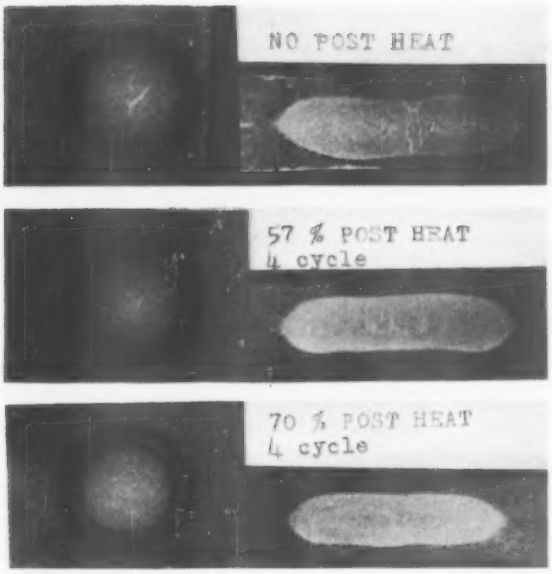


Fig. 11 Spot welds showing the effect of postheat on soundness. All welds are $^1/_2$ cycle, alternate polarity at $8^4/_7$-cycle frequency. Radiographs do not match the macrographs but are from the same panel of welds; macrographs, 5 ×

tion of forge force to the single force postheat cycle was beneficial from the strength standpoint.

Effect of Frequency (Weld Time)

Observation in the early stages of this investigation indicated that either half or full cycle wave welding could be used. No significant difference was found in the welds made with the half or full cycle wave. Increasing the weld time to five full cycles under identical conditions also failed to show any marked difference in weld size. It was felt that longer than $^1/_2$ cycle weld times were unnecessary and for the purposes of this investigation the $^1/_2$ cycle time was adequate. The data relating to weld time ($^1/_2$ cycle) effects on weld characteristics are given in Table 6. Frequency variations of 20, 12, $8^4/_7$, $6^2/_3$ and $5^5/_{11}$ cycles were used with forge force and with a combination of forge and postheat. Weld time variations did not eliminate cracking in the welds made with the dual force system at a forge delay of 3.3 cycles. However, when postheat currents of 57 and 70% were added, the cracking was completely eliminated at all frequencies. The magnitude of the current required to make the welds decreased with the frequency because of the longer current duration at the lower cycles of frequency. The effects of weld time on the strength characteristics could not be established at this time because of insufficient data. With the available results, it was concluded that frequency variations from 20 to $5^5/_{11}$ cycles could be satisfactorily used for welding 0.064-in. sheet although microscopic examination, Fig. 15, revealed wider heat-affected areas with longer weld time. Additional tests will be required to establish an optimum frequency for any particular sheet thickness.

Microscopic and Radiographic Examination

Since all the welds were radiographed, it was possible to produce matching radiographs and macrographs in most cases except the single force, postheat welds. All radiographs were enlarged two times. The black and whites are reversed because of direct printing from the

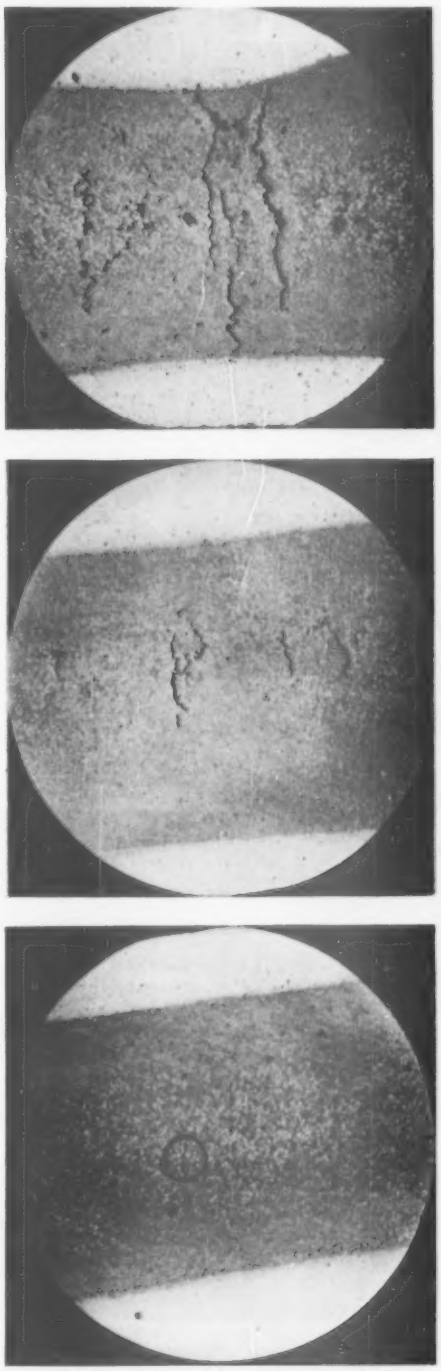


Fig. 12 Cracked welds of Fig. 11 at higher magnification (25 ×). Top: No postheat. Center: 57% postheat, 4 cycle. Bottom: Forged weld; note the fine crack

negatives. The usefulness of the radiographs in detecting soundness, expulsion and other features is illustrated in Fig. 7. The most unusual feature about the radiographs is the presence of a sharply defined ring at the periphery of the weld. This ring tends to be diffuse in small welds but becomes sharply defined as the weld increases. It has been established that the cause of this ring is the segregation of manganese. This is illustrated in Fig. 8, which shows the cubic shaped primary manganese particles at the periphery. The center of the weld nugget is shown in Fig. 9. It shows a cast dendritic structure with only occasional manganese particles. The segregation of manganese at the periphery may be due to either electromagnetic effects of the current or to the mechanical squeezing by the electrodes.

The appearance of some typical single force spot welds is shown in Fig. 10. The weld at the top shows cracks in both the macrograph and the radiograph. The cracks originated at the center of the welds and radiated outward. The remaining welds, made at the higher electrode forces, were all sound. The effect of postheat on the soundness of welds is illustrated in Fig. 11. Postheat of 57% reduced the cracking somewhat but 70% completely eliminated it. The appearance of the same cracks at higher magnification is shown in Fig. 12, together with a forged weld at the bottom with a crack so fine as to be barely detectable. This crack was visible in the radiograph. The appearance of typical sound welds made with both postheat and forge force are illustrated in Fig. 13. The heat-affected area adjacent to the spot, as well as the periphery of the spot, are shown in Fig. 14. The whiskerlike extensions radiating from the spots in the parent metal are eutectic stringers similar to the type seen in aluminum welds. At higher magnifications these streamers show up as a eutectic structure. The manganese segregation at the edge of the weld is also visible. Typical spot weld sections made with various frequencies are shown in Fig. 15. The shape of the weld made with

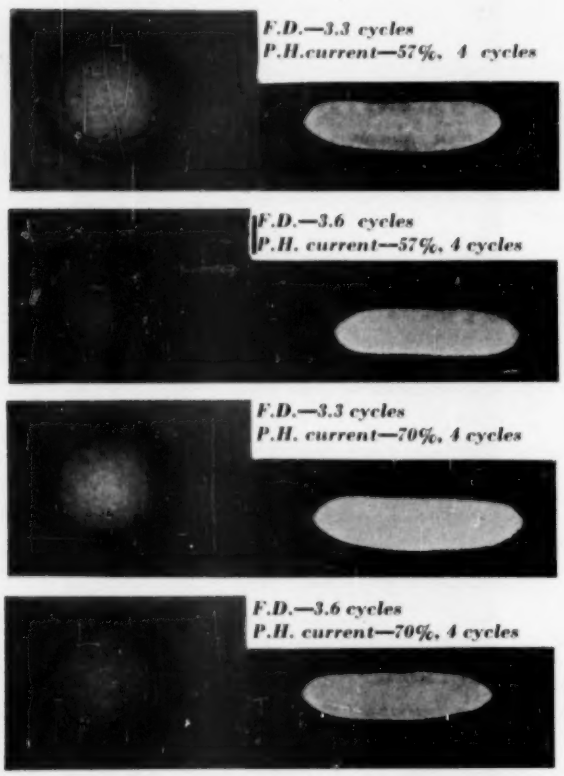


Fig. 13 Radiographs and macrographs of $^1/_2$-cycle spot welds with $8^1/_2$-cycle frequency and a combination forge and postheat. Radiographs, 2 ×; macrographs, 4 ×.

F.D.—Forge delay
P.H.—Post heat

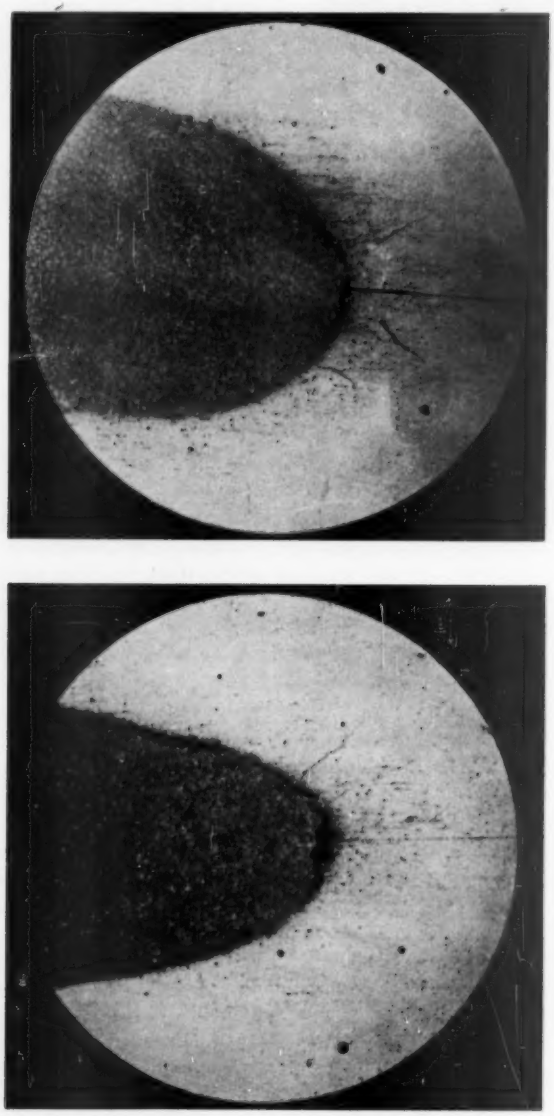
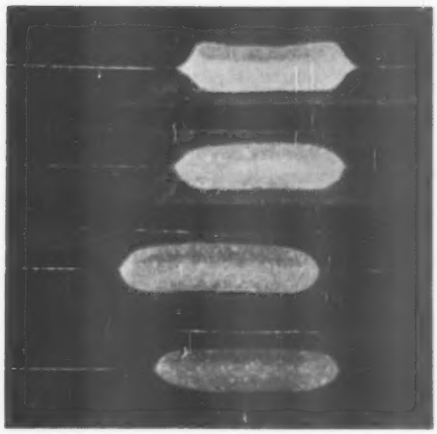


Fig. 14 Appearance of heat-affected area adjacent to the weld nugget. Eutectic extrusions radiate outward from the nugget. Manganese particles segregated at the periphery. Top: 70% postheat, 4 cycles, 25 ×; Bottom: Forge plus 70% postheat, 4 cycles, 25 ×

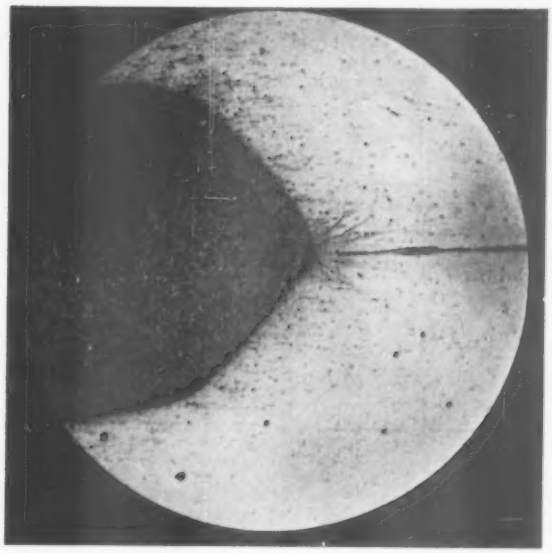


values of Hess[3] and co-workers indicates good agreement as shown below:

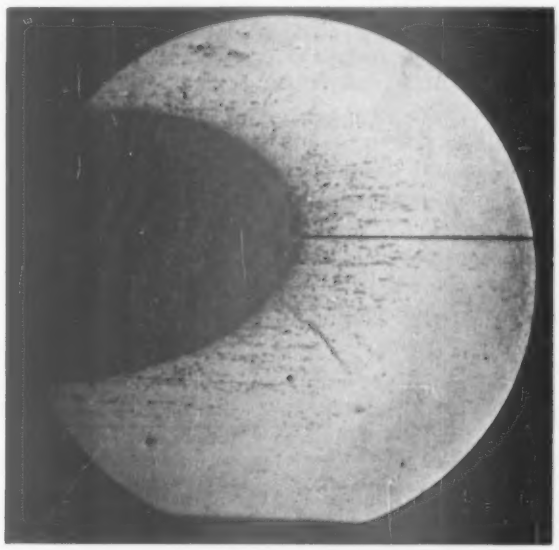
	Weld force	*Spot strength, lb*	*Spot diameters, in.*
Stored energy	2400	870–1040	0.29–0.33
Low frequency	2500	896–1075	0.30–0.38

However, it should be pointed out that the stored energy values are based on the hard rolled, whereas the low frequency are on the annealed sheet.

Fig. 15 Spot welds made with various frequencies: Top figure (left), 4 ×; 5% acetic acid etch; Bottom: magnification 25×



20 cycles



5 ⁵/₁₁ cycles

the 20-cycle frequency indicates unstable condition and therefore may not be desirable for this gage sheet. The effect of frequency on the heat-affected zone is shown in the two illustrations at the bottom taken at 25 ×. The 5⁵/₁₁ frequency weld shows more and considerably longer eutectic stringers, indicating the greater heating effect of the longer weld time. When postheat was applied to the 20-cycle welds, the tip was rounded out and the weld nugget became symmetrical.

DISCUSSION

It has been shown that satisfactory spot welds can be made in magnesium with the a-c low frequency converter type of welding system. Crack-free welds can be obtained with the single force system. An optimum force for the 0.064-in. sheet is indicated to be 2500 lb. Lower force values can be used to obtain sound welds but the critical range of conditions is too narrow for practical use. A comparison of the optimum frequency converter values with the electrostatic stored energy

The use of dual force (forge) permitted lower welding forces and secondary currents for sound, crack-free welds. The data have shown that with forge force the welding force may be half of that of the single force system and that the optimum ratios of weld to forge force lie in the neighborhood of 1:1½ to 1:2. At the latter ratio, a welding force of 1300 lb was used compared to 1600 lb with the 1:1½ ratio.

The point of application of the forge force was not especially critical and depended somewhat on the initial welding force. Examination of weld strengths indicated that forging did not increase the spot strengths to any significant degree.

When using postheat with a single force system, it was observed that crack-free welds could be obtained at lower welding forces and secondary currents, but at some sacrifice in spot strengths. At welding forces of 1700 and 1800 lb, sound welds were obtained when the postheat current was 70% of the weld current. When the postheat current was reduced to 57%, cracking was greatly reduced but not entirely eliminated. Postheat

time of at least 2 or 3 cycles was required for effective elimination of cracks. The loss in weld strength due to postheat ranged from 10 to 15%.

Postheat in combination with dual force permitted lower forge forces. The point of application of forge force was less critical for crack-free welds than without postheat. Sound welds were obtained at the $1:1^1/_2$ force ratio (1200–1920 lb) with postheat, whereas cracked welds were obtained without postheat. Again, a 70% postheat current at 2 cycles or longer was required for crack-free welds. Forge delays up to 4.6 were satisfactory. There was an indication that the weld strengths were only slightly affected by the postheat. Therefore, by the use of forge force in conjunction with postheat, the drawback of postheat alone due to the lowering of weld strengths was greatly reduced.

Brief tests with various frequencies showed that satisfactory welds in 0.064-in. magnesium sheet could be produced at any frequency from 20 to $5^5/_{11}$ cycles with either half or full cycle waves. However, the 20-cycle frequency resulted in irregularly shaped welds which might not be desirable because of possible variations associated with irregular welds. Weld strengths were not affected by frequency variations.

In conclusion, it has been shown that the three-phase frequency converter welder was satisfactory for spot welding magnesium. Any one of the following welding methods produced sound crack-free welds:

1. A single force system.
2. A dual force system.
3. A single force system with postheat.
4. A dual force sytem with postheat.

At the present time, there are insufficient data to cite any advantages of the low frequency system over the other systems of spot welding magnesium. Comparison with published information relative to spot strengths indicates no significant difference between low frequency and stored energy welds. Future tests are planned to investigate optimum conditions for other thicknesses and combinations.

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Metallurgical Effects of Oxygen Cutting Alloy Steel (Armor Plate)

Hardenability of armor plate due to oxygen cutting, physical characteristics of the cut edges and residual stresses resulting from the cutting operation

by F. C. Saacke

Abstract

The data presented in the following paper are taken from the results of an investigaton during World War II on the "Effects of Oxygen Cutting on the Weldability of Armor Plate" at the request of the National Research Council and has been published, in confidential form, in two Progress Reports, M-285 dated June 16, 1944, and M-633 dated Jan. 29, 1946, under NDRC Research Project NRC-71, OEMsr-1187.

Since these papers have been declassified, the subject matter relative to the metallurgical effects of oxygen cutting have been excerpted with specific attention being given to the microstructures and hardnesses developed by oxygen-cutting processes with various preheat fuels.

Effects of variables on the hardness of the outer Fusion Zone and the Hardened Base Metal Zone are discussed.

INTRODUCTION

ALTHOUGH the hardenabilities of carbon and alloy steels in heat-treating operations have been rather exhaustively studied, the hardenability of armor plate in the oxygen cutting operation does not seem to be so well understood. During World War II, this investigator had the opportunity of making extensive studies on the "Effect of Oxygen Cutting on Weldability of Armor Plate" and established a pattern that was representative of the hardenability of oxygen-cut steels at different zones near the cut edge. It had not been felt necessary to check all combinations of steel composition, cutting procedure and preheat conditions but enough was performed to indicate with reliability the characteristics to be expected under the different conditions followed in industrial practice.

The original investigation was specifically interested in determining the conditions necessary to develop cracks in armor plate when cutting, grooving or gouging with oxygen. It was found that many external factors besides internal corner radii and preheat temperature such as mechanical forming or straightening, residual plate stresses or stress relieving, steel cleanliness, etc., affect the development of cracks. Accordingly, it was concluded that the study of crack development would needlessly complicate the subject covered herein so that that part of the work was omitted from this presentation.

The subject of this paper will be restricted to that phase of the report covering the hardenability of the armor plate with some attention being given to the residual stresses resulting from the cutting operation on steel plate that was originally stress free. Since the original paper* has

* "Effect of Oxygen Cutting on Weldability of Armor Plate" (OD-136)—Part I, M-285—NDRC Research Project NRC-71, OEMsr-1187; Part II, M633—NDRC Research Project NRC-71, OEMsr-1187 "by Messrs J. J. Crowe, F. C. Saacke, C. J. Sullivan, and F. N. Stone."

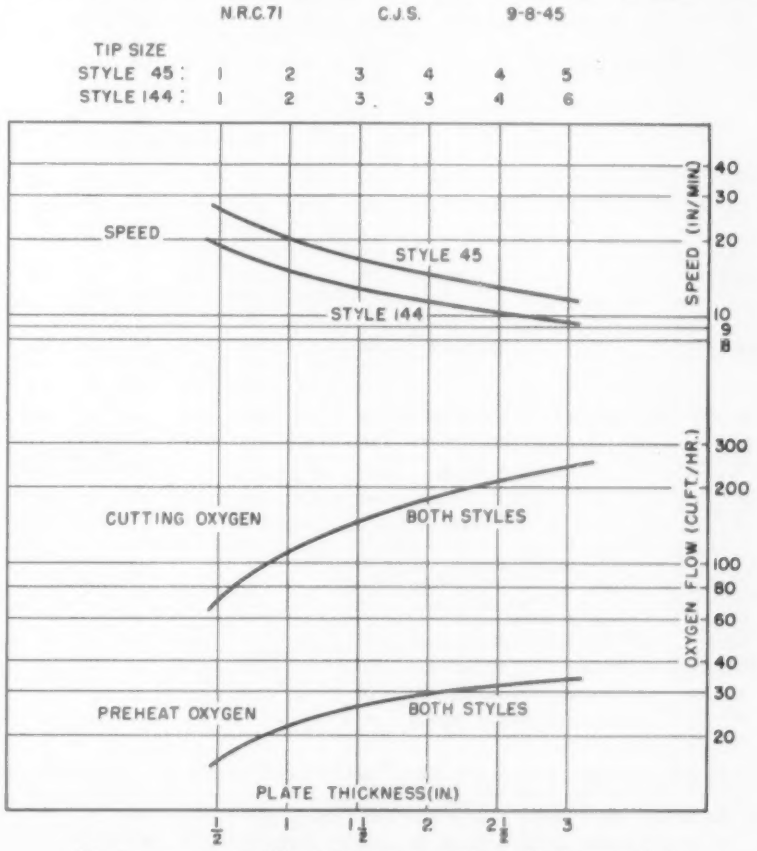


Fig. 1 Average conditions for oxygen cutting rolled armor plate

F. C. Saacke is Safety Director for the Air Reduction Co., New York, N. Y.

Presented at the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Mar. 15, 1953.

now been declassified and is available for reference, a large portion of the report referring to test methods, specimen preparation, and tabulating test data will not be reprinted. The subject report will confine itself to whatever data are of interest in clarifying the picture of what changes occur in the physical characteristics of armor plate at the edges of cuts made by the oxygen-cutting process.

The results of test are summarized by conclusions that the *hardness ranges* developed by oxygen cutting operations on armor plate are the same regardless of the nature of the cutting procedure, and that the *depth* of hardness penetration is the only variable affected-and usually because of a change of cutting speed. The principal conclusions are given at the end of the paper.

METHODS OF TEST

All cuts were made under representative conditions of tip size, oxygen flow, cutting speed and fuel gas consumption including acetylene, propane and hydrogen with preheat temperature ranging from −75 to 600° F.

Effects of heat input were determined by varying both cutting speeds and

Table 1—Chemical Composition of Rolled Homogeneous Armor Plate

PLATE THICKNESS	HEAT TREATMENT	HEAT NUMBER	C	Mn	P	S	Si	Cr	Zr	Mo	Ni	V		PLATE MARKINGS
Manufacturer "A"														
1-1/2"	HEAT TREATED	28352	.27	.86	–	–	.29	1.08	–	1.03	.96	.02		
"	" "	28354	"	"	–	–	"	"	–	"	"	"		
"	" "	28943	.24	.84	–	–	.24	.67	–	.51	.95			NR 11
"	" "	28944	"	"	–	–	"	"	–	"	"	–		NR 12A
"	" "	29103	"	"	–	–	"	"	–	"	"	–		NR 12B
"	" "	29104	"	"	–	–	"	"	–	"	"	–		NR 15
"	" "	28941	.25	.85	.013	.02	.25	.65	–	.48	.92	–		NR 10A
Manufacturer "B"														
1-1/2"	HEAT TREATED		.26	1.00	–	–	.20	.50	–	.40	1.00	–		NR 30B
"	AS ROLLED	920176	.27	1.24	.016	.024	.20	.72	–	.37	.58	.16Cu		
3"	AS ROLLED	970602	.27	1.37	.014	.020	.22	.77	–	.38	.58	.07Cu	.0013% BORON ADDED	1, 2 AND 3
"	a) HEAT TREATED	"	"	"	"	"	"	"	–	"	"	–	"	4, 5 AND 6
Manufacturer "C"														
1/2"	AS ROLLED	SCG 1156-1	.27	.98	.019	.024	.74	.56	.06	.21	–	–		
1"	" "	G9-12715	.30	.86 TO .89	.016 TO .019	.021 TO .028	.50 TO .56	.62 TO .65	.06 TO .07	.14 TO .15	–	–		
1-1/8"	" "	G9-12716	.29 TO .30	.90 TO .94	.017 TO .020	.025 TO .026	.55 TO .60	.67 TO .70	.07	.15	–	–		
1-1/2"	" "	G9-14413	.30 TO .31	1.03 TO 1.05	.019 TO .021	.019 TO .025	.65 TO .69	.69 TO .71	.08 TO .13	.16 TO .19	–	–		
"	b) AS ROLLED	SCG-1132-1	.29	1.14	.023	.026	.70	.66	.07	.26	–	–		
"	HEAT TREATED	SCG-2275-1	.28	1.10	.030	.022	.61	.67	.08	.21	–	.00067 BORON ADDED		
"	" "	9-03994	.30	.93	–	–	.71	.70	.09	.20	–	–		NR 23(29756)
"	" "	–	.28	.85	.026	.023	.86	.65	.08	.18				NR 50B
"	" "	–	"	"	"	"	"	"	"	"				NR 50C
Manufacturer "D"														
1-1/2"	HEAT TREATED	J 382	.28	.68	–	–	.22	–	–	.44	–	–		NR 53
2-1/2"	AS ROLLED	SCJ-2019-1	.26	1.72	.019	.015	.18	–	–	.50	4# GRAINAL X 79 PER TON			
3"	AS ROLLED	J-2039	.28	1.78	.023	.017	.21	–	–	.48	"	"	"	"
"	c) HEAT TREATED	SCJ-1362-2	"		"	"	"	–	–	"	"	"	"	A,B,C,D,E,F

a) WATER QUENCH AFTER 1-2/3 HRS AT 1625°F (6-1/3 HRS RISE) - AIR COOL AFTER 3 HRS. AT 1185°F (1-3/4 HRS RISE) BHN 269/277

b) STRESS RELIEVED AT 1150°F FOR 1-1/2 HOURS AND COOLED SLOWLY

c) WATER QUENCH AFTER 4 HOURS AT 1650°F - DRAW AT 1200°F FOR 5 HOURS AND WATER QUENCH (BHN 262)

acetylene preheat flows for different style tips and different plate thicknesses.

Effects of preheat gas were determined by comparing conditions that would produce square edge cuts with a "drag" that would just drop the cut.

Hardness specimens were taper ground and measured with a Vickers diamond and a 5-kg load (since hardness testers using loads below 1 kg were not yet available).

Residual stresses were measured by both relaxation methods and using resistance strain gages.

Depth of hardness gradients were measured at the top, middle and bottom of the cuts. Normally, the middle gradient was reported as being indicative of test conditions since the central portion of the cut was usually uniform for over 85% of the plate thickness and since the width of the heat-affected zone was usually a minimum at this point, producing maximum cooling rates and hardness zones.

RESULTS OF TEST

The details of the test methods and a more complete hardness traverse of each test cut are given in Part I of the report on "Effect of Oxygen Cutting on Armor Plate" (OD-136) made under NDRC research project NRC-71, OEMsr-1187 (M-285).

Table 2—Average Physical Characteristics of Rolled Armor Plate Heat Treated

Brinell Hardness	269/277
Yield strength, psi	113,400
Tensile strength, psi	129,900
Elongation, % in 2 in.	20.0
Reduction of area, %	65.6

DISCUSSION OF RESULTS

General Structure of Heat-Affected Zones

A typical heat-affected zone of an oxygen cut in steel is shown in Fig. 2. The width of the heat-affected zone varies from a maximum under the preheating flames to a fairly uniform minimum over approximately 85% of the cross section, increasing again in depth at the bottom of the cut where the plate geometry and insulation by hot slag increase the heat penetration.

Since the width of the heat-affected zone varies inversely with the cooling rate, the cooling rates are a maximum and the hardnesses of martensitic structures are the highest in the central part of the cut where the width is a minimum. For purposes of analysis, hardness gradients were measured in this center section. Where cutting speeds were critical, that is, with longer drags and somewhat irregular kerfs, the hardness gradient studies were taken at the section of minimum width.

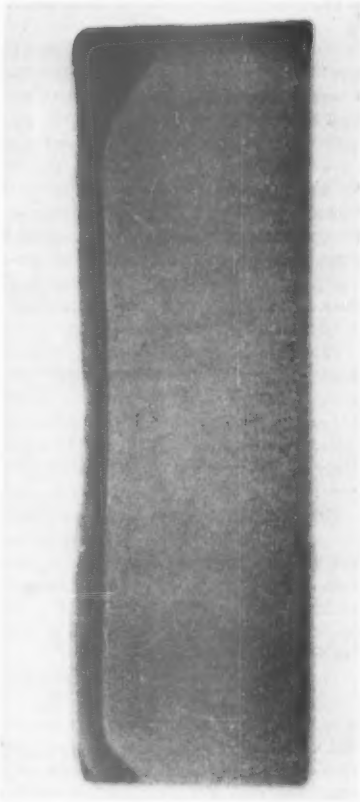


Fig. 2 Cross section of oxygen cut (70° F.) × 3

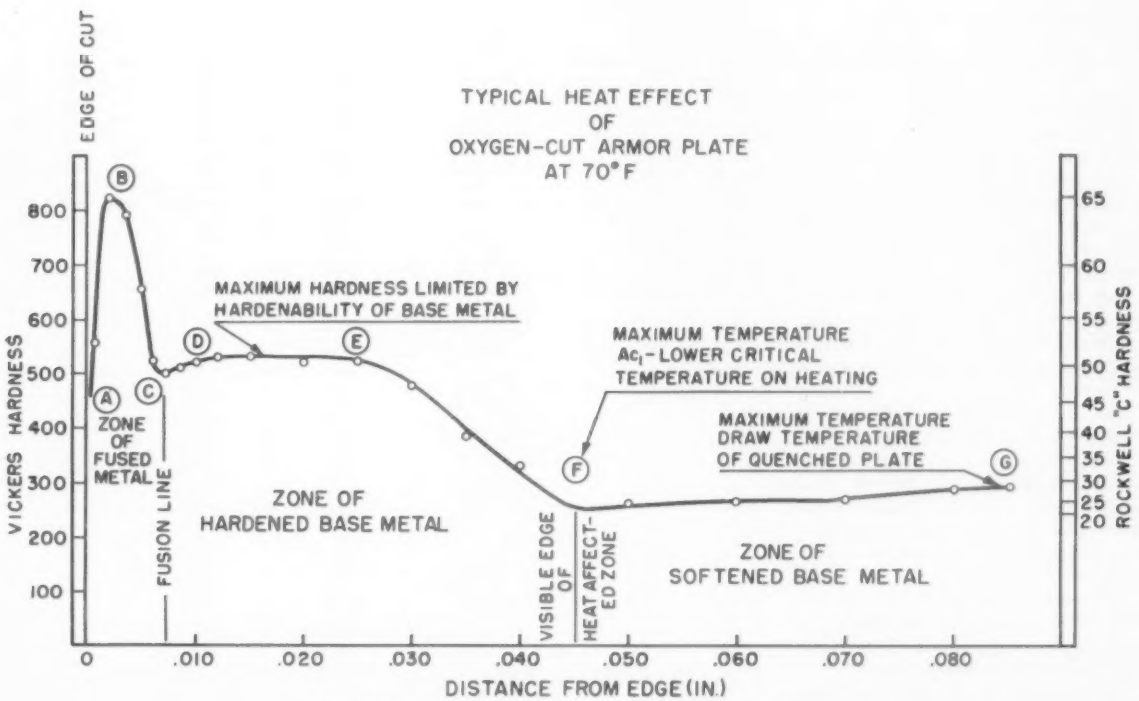


Fig. 3 Typical heat effect of oxygen-cut armor plate

The general shape of the hardness gradient curve may be seen in Fig. 3 (actually plotted from the results of test on Specimen 13). A study of the metallographic structures present indicates three distinct metallurgical zones.

The outer zone on the cut edge includes all metal that has been melted in the course of making the cut and is called the Fusion Zone. It is thin, ranging in thickness on the specimens tested from 0.001 to 0.015 in. In many steels, particularly the carbon steels, this zone may be missing entirely, or present in short, widely separated patches.

This zone has a cast structure of higher maximum carbon and alloy content than the base metal. On the extreme outer surface, it very often shows a ferritic or austenitic structure (not identified positively) of low hardness at (*A*) (see Fig. 3) increasing as the carbon content of the martensite increases to a maximum hardness at (*B*) well above the maximum considered possible with an alloy steel of this carbon content, and again decreasing in hardness to the Fusion Line at (*C*), the boundary between the metal surface and the unmelted base metal.

The second zone called the Hardened Base Metal Zone extends from the Fusion Line at (*C*) where the steel shows hardnesses slightly below the normal maximum to the visible edge of the heat-affected zone at (*F*) which is governed by the location of temperatures in excess of the lower critical on heating. This covers a section from (*C*) to (*D*) where the steel has been heated to temperatures close to the melting point with a slight loss in maximum hardness; a section from (*D*) to (*E*) where the normal hardnesses on rapid cooling are obtained, and a section from (*E*) to (*F*) where probably incomplete diffusion of the carbides in the austenite phase has occurred prior to quenching.

Beyond these two zones, a third zone called the Softened Base Metal Zone is found in heat-treated armor plate which is caused by heating below the lower critical temperature but still in excess of the previous draw temperature of the quenched and drawn steel.

This outer zone is being called the Fusion Zone since it covers the region where melted metal that was not washed away in the cutting stream solidified while still on the surface.

The photomicrograph of Fig. 4, although somewhat similar to that of Fig. 8, shows a columnar structure in the original austenitic matrix of this section which bends downward in the direction of cutting. (The microscope reverses this direction and makes the crystals tilt upward in the photomicrograph.)

The specimen in Fig. 5 was purposely relief polished and photographed at an angle to emphasize the lack of rolled steel inclusions in the cast surface layer.

The structures of the armor plate cuts made at room temperature are shown in

Table 3—Oxygen Cuts in 1½ In. Heat Treated Armor Plate

RESULTS OF TESTS

SPEC. NO.	CUTTING SPEED IN./MIN.	ACET. FLOW C.F.H.	CUTTING OXYGEN FLOW C.F.H.	DRAG IN.	CRACKS	MAXIMUM VICKERS (5KG.) FUSION ZONE	HEAT AFFECTED ZONE	NORMAL ZONE	DEPTH FUSION ZONE IN.	DEPTH AC_1 IN.	DEPTH 250 VICKERS IN.
ARMOR PLATE AT 70°F.											
5	12.54	23.2	165	.12	NONE	778	518	260	.0050	.042	.042
6	13.48	"	"	.17	"	788	524	270	.0060	.039	.040
7	14.55	"	"	.38	"	798	524	250	.0068	.045	.045
8	15.14	"	"	.41	"	788	508	245	.0098		
9	14.55	28.4	"	.30	"	788	524	270	.0076	.042	.044
10	15.14	"	"	.52	"	798	518	260	.0084	.047	.047
11	15.79	"	"	.62	"	798	518	261	.0078	.040	.040
12	13.48	39.8	"	.54	"	781	524	254	.0100	.078	.079
13	15.14	"	"	.57	"	788	547	282	.0084	.045	.045
15	12.54	25.8	143	0	"	632	530	256	.0030	.032	.034
16	15.79	"	"	.03	"	788	540	261	.0045	.048	.047
14	17.27	"	"	.38	"	808	530	256	.0075	.050	.050
17	14.55	29.9	"	.02	"	810	540	254	.0070	.048	.046
18	15.79	"	"	18	"	808	541	258	.0045	.035	-
19	17.27	"	"	19	"	808	518	254	.0075	.042	.040
20	15.14	39.8	"	.23	"	808	524	250	.0100	.048	.047
21	15.79	"	"	.14	"	808	513	248	.0100	.060	.060
22	17.27	"	"	12	"	778	538	254	.0070	.047	.047
ARMOR PLATE AT 30°F.											
23	12.54	23.5	167	12	NONE	808	530	280	.0055	.053	.052
25	15.79	"	"	.02	"	830	541	255	.002 .015	.035 .069	.035 .069
24	14.55	"	"	0	"	798	524	243	.0043	051	.051
26	17.27	"	"	.08	"	798	524	250	.001 .012	.026 .049	.024 .050
27	14.55	28.4	"	.04	"	830	524	280	.0064	.045	.045
28	15.79	"	"	.02	"	819	530	265	.0056	.043	.043
29	17.27	"	"	12	"	808	524	260	.0060	042	.038
30	14.55	39.4	"	.10	"	819	535	252	.0050	.039	.038
31	15.79	"	"	.04	"	808	530	256	.004 .012	053 076	.053 .077
32	17.27	"	"	17	"	835	535	282	.0064	.038	.038
33	12.54	24.4	165	19	"	819	518	245	.0085	.055	.055
34	13.48	"	"		"	830	524	270	.0140	-	.063
35	14.55	"	"		"	830	513	255	.004 .012	.065	.061 .069
ARMOR PLATE AT 75°F.											
36	12.54	40.5	167	.03	NONE	788	535	254	.0048	.042	.042
37	14.55	"	"	.15	"	841	530	252	.0080 .0036	.062 035	.061 .034
38	14.55	"	"	.05	"	830	535	278	.0064	.046	.046
ARMOR PLATE AT 300°F.											
39	12.54	23.5	167	.07	NONE	798	493	250	.0085	.057	.057
40	14.55	"	"	-.03	"	808	508	270	.0068	.050	.050
41	15.79	"	"	-.02	"	808	498	240	.0056	.050	.050
42	17.27	"	"	-.02	"	788	524	280	.0069	.045	.045
43	12.54	28.4	"	0	"	739	473	246	.0064	.053	.050
44	14.55	"	"	.01	"	788	503	248	.0048	.050	.049
45	15.79	"	"	.02	"	819	503	252	.0048	.046	.045
46	17.27	"	"	.06	"	788	503	248	.0048	.044	.044
65	12.54	39.8	"	.03	"	778	518	280	.003	.045	.045
66	14.55	"	"	.03	"	798	530	291	.0044	.042	.041
67	15.79	"	"	.02	"	788	513	250	.0062	.037	.037
68	17.27	"	"	.03	"	-	-	-	-	-	-
69	19.12	"	"	.08	"	788	518	250	.0056	.038	.035
70	21.45	"	"	.20	"	808	508	258	.0070	.038	.035
ARMOR PLATE AT 600°F.											
47	12.54	23.5	167	0	NONE	625	402	254	.0024	.050	.053
48	14.55	"	"	0	"	611	446	265	.0029	.042	.041
49	15.79	"	"	.01	"	748	446	254	.0058	.045	.050
50	17.27	"	"	.03	"	524	442	274	.0020	.035	.036
51	19.12	"	"	.06	"	695	503	267	.0032	.033	.033
52	21.45	"	"	.13	"	655	459	274	.0046	.037	.037
53	12.54	28.4	"	0	"	565	409	245	.0053	.056	.056
54	14.55	"	"	.03	"	712	417	250	.0041	.050	.049
55	15.79	"	"	.05	"	758	442	246	.0048	.048	.046
56	17.27	"	"	.07	"	703	464	263	.0046	.041	.041
57	19.12	"	"	.12	"	721	524	291	.0041	.037	-
58	21.45	"	"	19	"	524	394	260	.0048	.039	.039
59	12.54	39.8	"	0	"	518	394	241	.0028	.052	.050
60	14.55	"	"	.03	"	655	391	246	.0041	.049	.048
61	15.79	"	"	0	"	547	394	246	.0027	.042	.042
62	17.27	"	"	.04	"	632	425	245	.0030	.043	.043
63	19.12	"	"	.09	"	640	446	254	.0034	.039	.039
64	21.45	"	"	.04	"	679	437	245	.0070	.040	.040

*Maximum hardness on edge of cut - difficult to measure accurately with 5 Kg. Load and 5 to 1 Slope.

the photomicrographs of Figs. 2 through 13.

HARDNESS VARIATIONS IN OXYGEN CUTS

A typical heat-affected zone of an oxygen cut in armor plate preheated to 600° F is shown in Fig. 14. In general, there is no difference in appearance of cuts in armor plate made at 70° F or at 600° F. Depths of heat-affected zones are not visually different which would be expected from the relatively small drop in hardness developed by 600° F preheat.

The metallographic structures of oxygen cuts on armor plate preheated to 600° F are shown in Figs. 15 through 21.

The hardnesses developed by the oxygen-cutting operation follow the general pattern outlined in the graph of Fig. 3. Hardening in the surface fusion zone and in the underlying heat-affected zone is determined by the plate analysis and the thermal cycle set up by the combination of plate geometry, heat input and cutting speed. Unless, the plate to be cut is preheated, the cooling rates are sufficiently in excess of the critical cooling rates to induce hardening to a martensitic structure in the heat-affected zone wherever critical temperatures have been exceeded.

The use of different fuel gases such as propane and hydrogen have no effect on the hardenability of the base metal since full hardening is achieved in the heat-affected zone in each case (see Fig. 26 and Table 5).

Actually, the maximum hardnesses encountered in the heat-affected zones of base metal cut without preheat vary only slightly from the maximum hardnesses possible as determined from the curve of maximum hardness versus carbon content published by Burns, Moore and Archer in

Table 4—Straight-Line, Square-Edge Cuts in Rolled Armor

RESULTS OF TESTS

SPEC. NO.	HEAT NO.	PLATE SIZE (IN.)	TIP SIZE	PLATE TEMP. (°F)	CUTTING SPEED (IN/MIN)	ACET. FLOW C.F.H.	CUTTING OXYGEN C.F.H.	MAXIMUM VICKERS (5KG) FUSION ZONE	MAXIMUM VICKERS (5KG) HEAT AFFECTED ZONE	MAXIMUM VICKERS (5KG) NORMAL ZONE	DEPTH FUSION ZONE (IN)	DEPTH A_1 (IN)	CRACKS
					Mfr. "C" UNHEAT TREATED PLATE								
123	SCG. 1156	1/2"x20x20	45 #1	70°F	25.0	17.7	57	-	541	206	.001	.020	NONE
124	"	"	"	"	"								
125	"	"	144 #1	"	19.0	17.7	57	703	553	219	.002	.031	"
126	"	"	"	"	"								
95	G9-12715	1"x20x20	45 #2	"	21.5	21.5	120	819	524	209	.004	.025	"
96	"	"	"	"	"	33.4	"	748	535	197	.002	.025	"
97	"	"	144 #2	"	14 6	25.0	"	655	524	198	.006	.045	"
98	"	"	"	"	15.8	36.5	"		513	210	.005	.049	"
91	G9-12716	1-1/8"x20x20	45 #2	"	19.1	21.5	130	739	473	194	.007	.035	"
92	"	"	"	"	"	33.4	"	632	464	194	.003	.023	"
93	"	"	144 #2	"	13.5	25.0	"	618	468	202	.006	.048	"
94	"	"	"	"	14.6	36.5	"	758	437	210	.006	.045	"
71	9-14413	1-1/2"x36x36	45 #3	"	17.3	25.8	183	841	541	261	.002	.025	"
74	"	1-1/2"x6x6	"	"	"	"	"	852	578	240	.004	.027	"
75	"	"	"	"	"	39.8	"	830	584	233	003	.030	"
76	"	"	144 #3	"	12.5	24.4	165	739	584	229	.006	.042	"
77	"	"	"	"	13.5	39.8	"		578	249	.007	.062	"
					Mfr. "C" HEAT TREATED PLATE								
115	SCG. 2275	1-1/2"x6x6	45 #3	-75°F	14.6	25.8	165	695	553	260	.002	.030	"
116	"	"	"	"	"	39.9	"	687	571	260	.003	.030	"
117	"	"	144 #3	"	12.5	25.8	"	841	571	260	.008	.045	"
118	"	"	"	"	13.5	39.9	"	808	553	260	.003	.033	"
83	NR 50C	"	45 #3	70°F	17.3	25.8	183	830	578	260	.004	.034	"
84	"	"	"	"	"	39.8	"	739	578	260	.001	.025	"
87	"	"	144 #3	"	12.5	24.4	165	739	578	260	.005	.066	"
90	"	"	"	"	13.5	39.8	"	553	565	260	.015	.068	"
107	NR 50B	"	45 #3	300°F	17.3	25.8	183	597	518	278	.003	.028	"
108	"	"	"	"	"	39.8	"			274			"
109	"	"	144 #3	"	12.5	24.4	165			274			"
110	"	"	"	"	13.5	39.8	"	358	464	274	.005	.040	"
99	"	"	45 #3	600°F	17.3	25.8	183	305		278			"
100	"	"	"	"	"	39.8	"		361	278	.003	.024	"
101	"	"	144 #3	"	12.5	24.4	165	530	343	278	.003	.045	"
102	"	"	"	"	13.5	39.8	"	334	352	285	.008	.055	"
					Mfr. "D" HEAT TREATED PLATE								
82	NR 53	"	45 #3	70°F	17.3	25.8	183	798	590	281	.002	.033	"
81	"	"	"	"	"	39.8	"	841	565	278	.004	.033	"
85	"	"	144 #3	"	12.5	24.4	165	819	503	271	005	.042	"
89	"	"	"	"	13.5	39.8	"	841	565	271	.010	.059	"
111	"	"	45 #3	300°F	17.3	25.8	183	712	530	260	.003	.028	"
112	"	"	"	"	"	39.8	"	739	524	278	.002	.028	"
113	"	"	144 #3	"	12.5	24.4	165	758	530	281	.001	.034	"
114	"	"	"	"	13.5	39.8	"	663	541	271	.006	.052	"
103	"	"	45 #3	600°F	17.3	25.8	183	370	483	268	.004	.042	"
104	"	"	"	"	"	39.8	"	358	473	271	.004	.045	"
105	"	"	144 #3	"	12.5	24.4	165	391	450	271	.005	.070	"
106	"	"	"	"	13.5	39.8	"	384	498	271	.013	.100	"
	SCJ-1362	3"x20x20	45 #6	70°F	11.9	30.4	265	841	571	260	.006	070	"

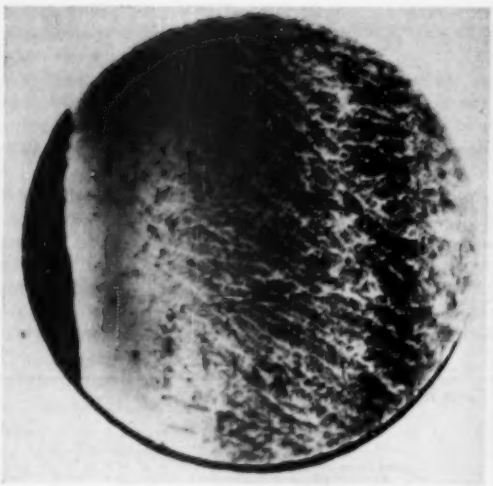


Fig. 4 Section of cut edge (70° F). × 350

Fig. 5 Section of cut edge (70° F), unetched. × 8

the 1938 *ASM Transactions* (see Fig. 22) and these variations are mainly due to structural differences in the plate particularly in unheat-treated armor.

The composition of the fusion zone is a function of the melting conditions under the cutting tip as well as the original plate analysis. Interest in the composition of this fused layer is due solely to the need for predicting the hardenability of a surface layer which may be as much as 0.015 in. in thickness. The composition of this fusion zone was not determined chemically but was interpreted in the light of hardness tests and microscopic examination.

There seems to be no reason to doubt the presence of carbon in higher concentrations than is present elsewhere in the base metal. The higher carbon surface structures are visible under the microscope and predictable from the higher surface hardnesses. Tests on plain carbon steels, such as SAE 1020, indicate the same thin carburized surface layer (see Fig. 23).

The cause of this surface carburization phenomena lies most likely in the selective oxidation of the iron in the path of the cutting-oxygen stream with a gradual increase in the carbon content of the liquid iron on the face of the cut. The cuts made with hydrogen as a fuel gas (see Table 5) show the same increase in surface hardness beyond the capabilities of the normal carbon content of the base metal indicating that the higher carbon comes from the steel itself.

The composition of this surface layer should be considered when methods of controlling the hardness of oxygen-cut plate edges are being formulated. The maximum hardnesses in the fusion and hardened base metal zone of preheated armor plate have been plotted graphically in Fig. 24. Apparently, preheating the base metal to elevated temperatures can sometimes be relied upon, within limits to control the hardnesses developed in the fusion zone as well as the hardnesses in the heat-affected zone (see Table 4). In this respect, the use of nickel in the armor plate, since it is apparently more readily concentrated in the fusion zone by the oxygen cutting process, increases the hardenability of the fusion zone at

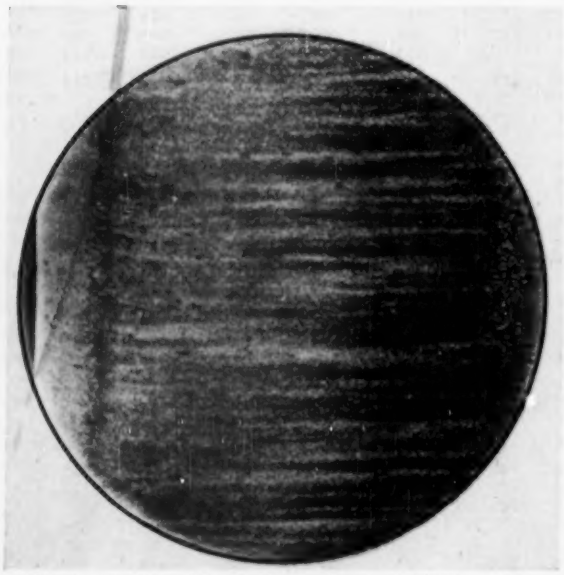


Fig. 6 Section of heat-affected zones (70° F). × 50

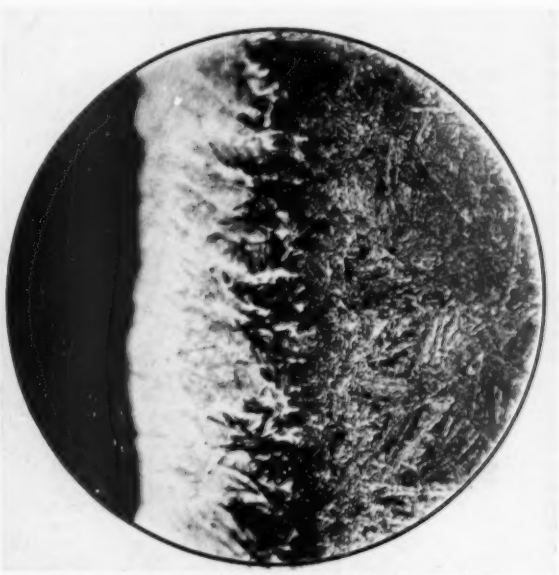


Fig. 7 Section of fusion zone (70° F). × 350

Table 5—Hardnesses of Cuts Using Acetylene, Propane and Hydrogen

	Tip No. 3		Tip No. 4			Tip No. 5		
	Acetylene	*Propane*	*Acetylene*	*Propane*	*Hydrogen*	*Acetylene*	*Propane*	*Hydrogen*
Maximum Vickers hardness in heat-affected zone								
Top	513	418	513	524	535	518	524	518
Middle	553	553	530	564	530	518	524	547
Bottom	541	513	513	565	535	524	483	524
Depth of heat-affected zone in inches								
Top	0.106	0.084	0.110	0.116	0.080	0.092	0.084	0.070
Middle	0.030	0.042	0.034	0.036	0.030	0.024	0.031	0.030
Bottom	0.054	0.048	0.050	0.050	0.042	0.048	0.050	0.054

Surface hardnesses of 750 Vickers in the Fusion Zone were noted on all cuts, these exceed the maximum hardenability of the base metal.

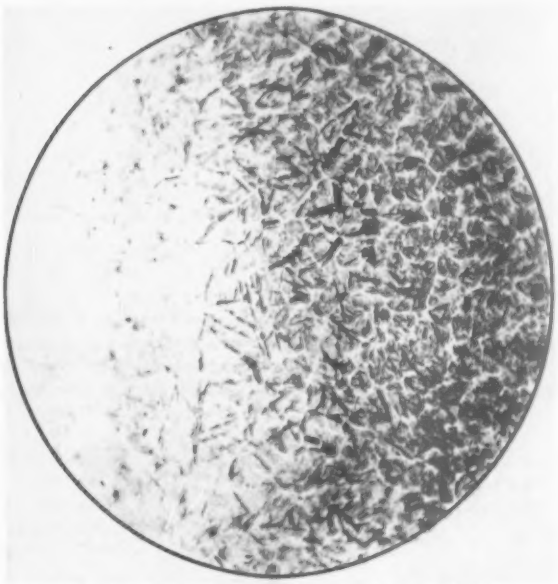


Fig. 8 Section of fusion line (70° F). × 350

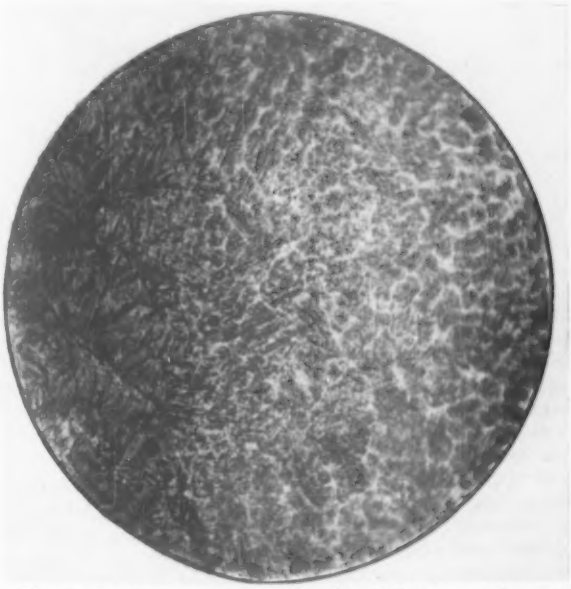


Fig. 9 Section at start of hardened base metal zone. × 350

Fig. 10 Section of hardened base metal zone (547 Vickers). × 350

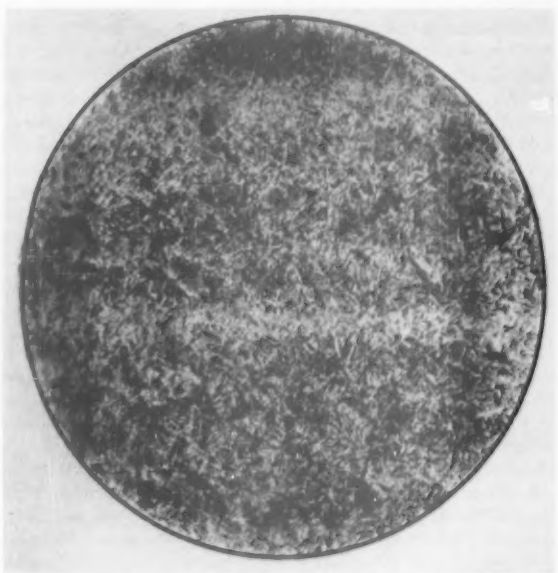


Fig. 11 Section of hardened base metal zone (350 Vickers). × 350

Fig. 12 Section at end of hardened base metal zone. × 350

Fig. 13 Section of normal structure in armor plate. × 350

both normal and elevated temperatures. The manganese-molybdenum steel armor plate without nickel or chromium shows the lowest hardenability in the fusion zone when the plate is preheated before cutting.

The maximum hardnesses in this fusion zone are about 50% higher than in the hardened base metal zone.

Apparently, variations in cutting oxygen consumption, fuel gas consumption and speed with plates at subnormal and room temperatures were unable to reduce the cooling rate sufficiently to avoid full hardening, since the maximum hardnesses in the fusion zone in all cases came within 778/830 Vickers range while the maximum Vickers hardnesses in the hardened base metal zone fall within the 498/540 range.

The effects of preheat, cutting speed and acetylene flow are best illustrated in the results of test at 600° F (see Table 3).

Figure 25 represents the effect of furnace tempering specimens of nickel-chrome-moly armor plate of 0.24 carbon after quenching from 1700° F. At 600° F, the tempered specimens show 440 Vickers, a drop of 80 points from a maximum of 520 Vickers. The same armor when preheated to 600° F and oxygen cut shows maximum hardnesses in the hardened base metal ranging from 391 to 524 and averaging 435 over the range of data studied.

Variations in preheat, cutting speed, and acetylene do not affect the hardness developed by oxygen cutting until the preheat temperature exceeds 300° F.

As the acetylene consumption varies from 0.23 to 0.54 cu ft of acetylene per lineal inch of cut, the maximum base metal hardnesses at 600° F preheat range from approximately 500 at the higher speeds and lower acetylene flows to approximately 400 at the lower speeds with the higher acetylene flows.

At the same time, Fig. 24 shows that the Fusion Zone hardnesses show a comparable range of hardnesses about 50% higher even at 600° F.

STUDY OF OXYGEN CUTTING WITH ACETYLENE, PROPANE AND HYDROGEN

It was concluded in analyzing the metallurgical structures developed in the oxygen cutting of armor plate, first, that the high fusion zone hardnesses resulted from an increase in carbon and alloy content caused by the preferential oxidation of iron in the oxygen cut and should be independent of preheat fuel gases. Secondly, it seemed obvious that the critical cooling rates over the major portion of the cut edge were dependent upon cutting speeds (and preheat temperatures) rather than preheat fuel gases.

These conclusions were drawn as the result of making comparative cuts using acetylene, propane and hydrogen. The general quality of the cut surface and particularly a uniform, high quality of top edge were made the basis of comparison among the various cuts. Conditions of the cuts were maintained uniform for each of the preheat fuels. Speed of cutting was 9½ ipm, cutting oxygen flow was 180 cfh and preheat flows were roughly 18 cfh for acetylene, 7 cfh for propane and 30 cfh for hydrogen. Style 164 tips were used for acetylene and hydrogen and Style 7888 for propane. These preheat flows were determined from experience to give the same quality of cut.

Hardness gradients were measured at the top, middle and bottom of the cut surfaces. As in all oxygen cuts on armor plate, maximum hardnesses and minimum depths of heat-affected zones were found

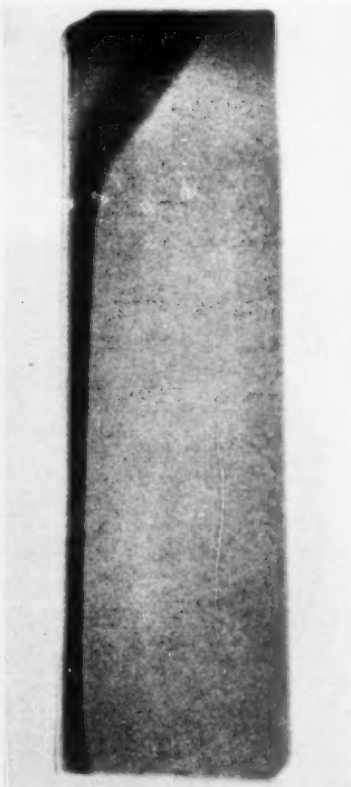


Fig. 14 Cross section of oxygen cut (600° F). × 3

Fig. 15 Section of heat-affected zones (600° F). × 50

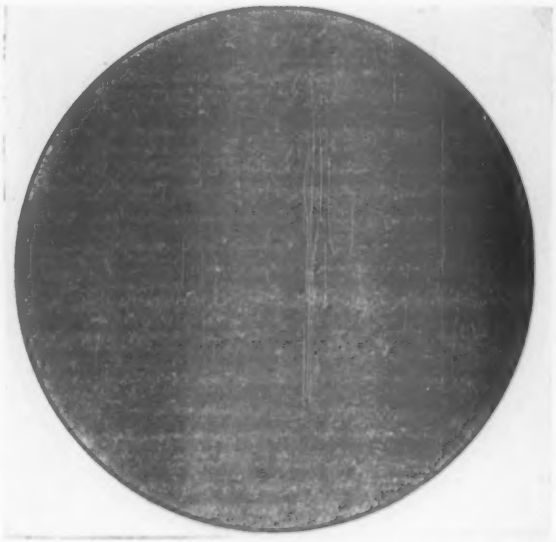


Fig. 16 Section at end of hardened base metal zone (600° F). × 50

at the middle of the three locations examined. There were not observable differences in surface condition or internal hardness characteristics among any of the preheat fuels. Maximum hardness, depth of heat-affected zone and quality of surface were similar, within the limits of experimental error, regardless of preheat fuel used. The maximum hardnesses of the heat-affected zones at the center section, which represented 85% or more of the entire cut, all showed 541 Vickers ±23 which is well within the range of experiperimental error, regardless of the preheat fuel used.

The Fusion Zones also were checked and each showed surface hardnesses approximately 50% in excess of the maximum noted in the hardened base metal zone; the cuts with hydrogen as a preheat fuel gas showing the same high increase in hardness as the acetylene and propane which would confirm the conclusion that the carbon causing high surface hardness does not come from the preheat gas used.

The comparison of hardness depths indicates little or no significant differences between the three preheat fuel gases except at the top of the cut and serves as a reminder that the cutting speed is dependent upon the rate with which iron will oxidize in an oxygen atmosphere and that the preheat gases function solely in precison cutting to maintain the start of the cut at the upper surface.

RESIDUAL STRESS

For a detailed study of the methods of determining residual stress and the effects of plate stresses, the reader should refer to the original NRC reports covering the effects of oxygen cutting and grooving in causing cracks in armor plate. The present paper is intended to indicate the general conditions following simple straight line cuts.

The specimens of Fig. 27 were prepared from the same cut and the right-hand specimen stress relieved before machining. Both specimens were machined on a shaper by the same procedure to indicate that the method of machining was not introducing stresses. The resultant warp-

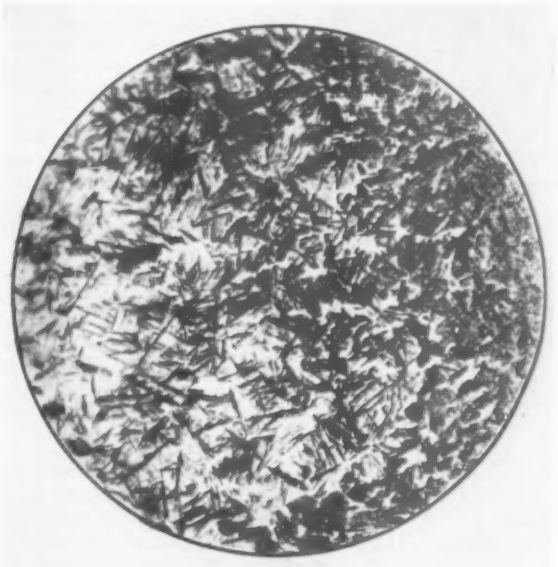


Fig. 17 Section of fusion zone (600° F). × 350

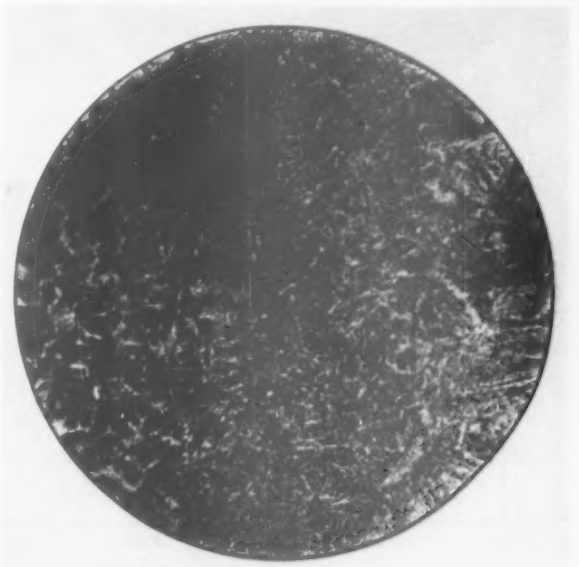


Fig. 18 Section of fusion line (600° F). × 350

Fig. 19 Section of hardened base metal zone (400 Vickers) (600° F). × 350

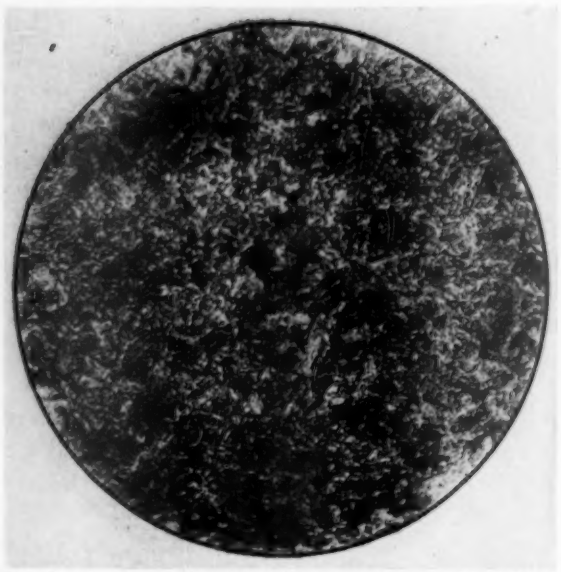


Fig. 20 Section of hardened base metal zone (300 Vickers) (600° F). × 350

Fig. 21 Section at and of hardened base metal zone (600° F). × 350

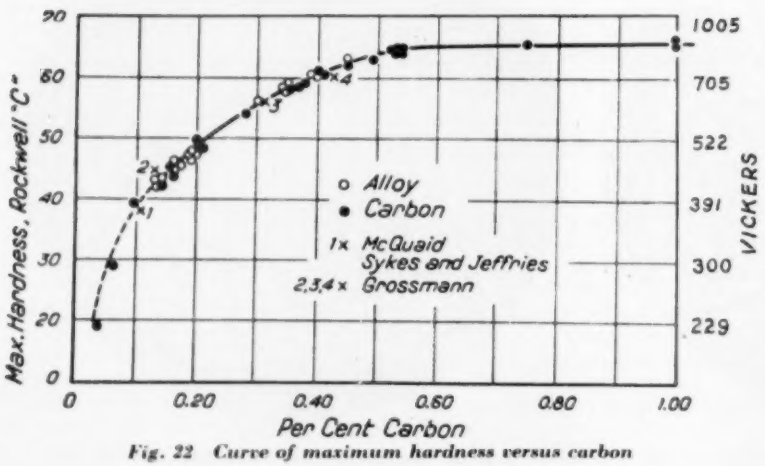


Fig. 22 Curve of maximum hardness versus carbon

age of the nonstress-relieved member shows first the presence of a surface structure of lower density that develops a high compression stress since the cooling conditions if applied to an uncut edge would quite obviously leave the surface with residual tension stresses. The residual surface compression stresses are equally obviously caused by the martensitic structures developed by the thermal cycle, since martensite is known to be of lower density than pearlite. The thickness of the section indicates the relative shallowness of this hardened structure and the steepness of the stress gradients present.

The graph and table of Fig. 28 show that surface compression stresses can rise to at least 130,000 psi and should satisfy fabricators that the oxygen-cutting operation in itself does not normally cause cracks, since the tensile stresses mainly responsible for causing cracks in straight line cutting are probably the residual stresses present prior to cutting. Naturally, inside corner cuts might be expected to crack when corner radii and preheat temperatures are below certain critical values, and further information on such conditions is available in the original NRC reports covering this subject.

All oxygen cut specimens regardless of preheat temperatures varying from −75° F to 600° F or regardless of cutting procedure showed surface longitudinal compression stresses ranging between 38,000 and 131,000 psi.

CONCLUSIONS

Hardening in the surface-fusion zone and in the underlying heat-affected zone of carbon and alloy steels is normally determined by the plate composition and by the thermal cycle established by the combination of plate geometry, heat input and cut-

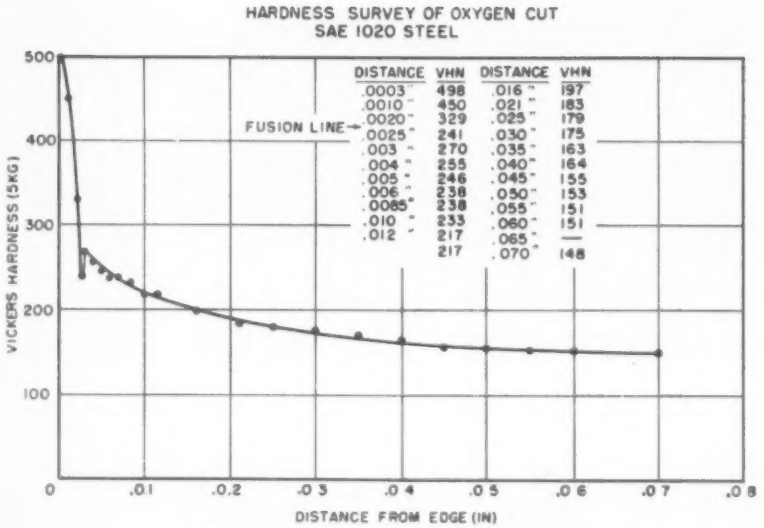
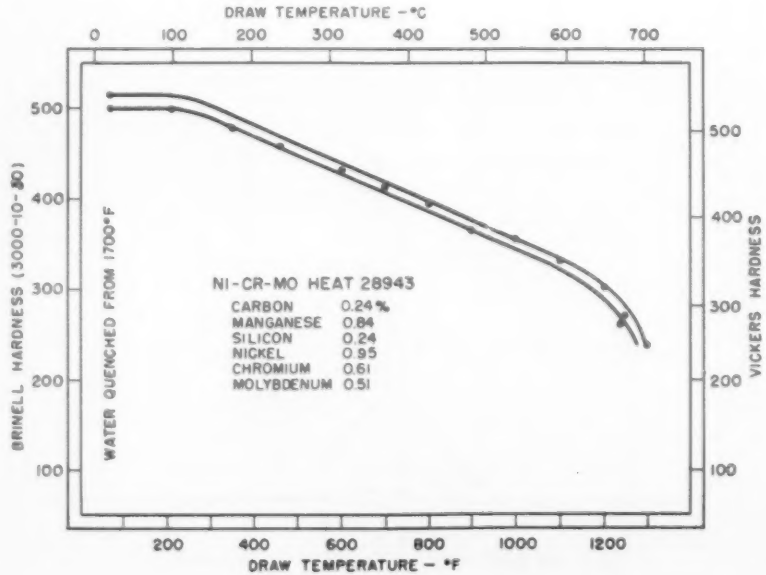


Fig. 23 Hardness survey of oxygen cut



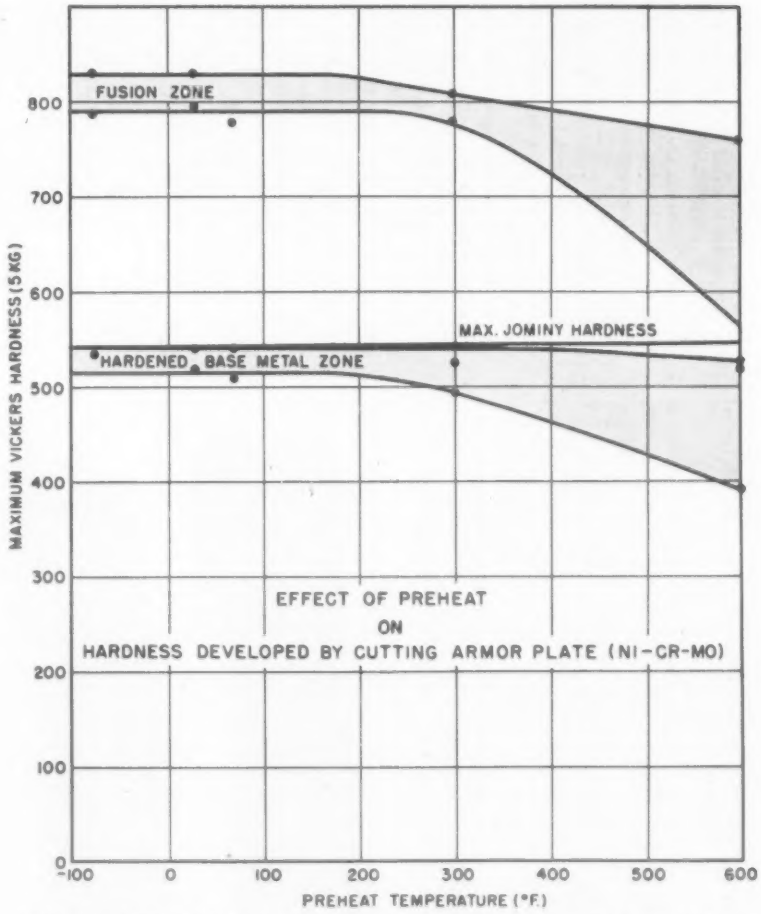


Fig. 24 Effect of preheat on hardnesses developed by cutting armor plate

Fig. 25 (left). Oil quenched and drawn armor plate hardnesses

Operating Conditions with Oxygen-Cut Armor Plate (1½ In.) for Different Preheat Fuels

Cut No.	*Preheat fuel*	*Tip*	*Fuel ow, cfh*	*Preheat oxygen flow, cfh*	*Cut oxygen flow, cfh*	*Speed of cut, ipm*
P-5-5	Propane	7888#5	7	31	180	10
H-5-7	Hydrogen	164#5	30	17	180	9.5
A-5-9	Acetylene	164#5	17	22	180	9.5
A-4-11	Acetylene	164#4	12	17	180	9.5
H-4-13	Hydrogen	164#3	30	15	180	9.5
P-4-14	Propane	7888#4	7	29	180	9.5
A-3-15	Acetylene	164#3	18	20	180	9.5
P-3-17	Propane	87#3	7	31	180	9.5

Fig. 26 Operating conditions for different preheat fuels

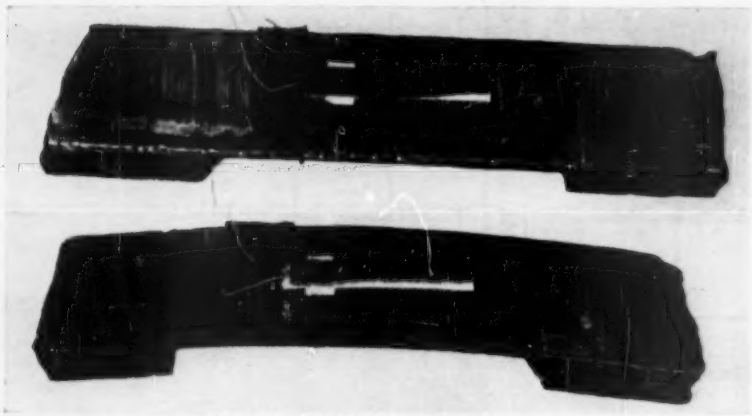


Fig. 27 Residual stress specimens

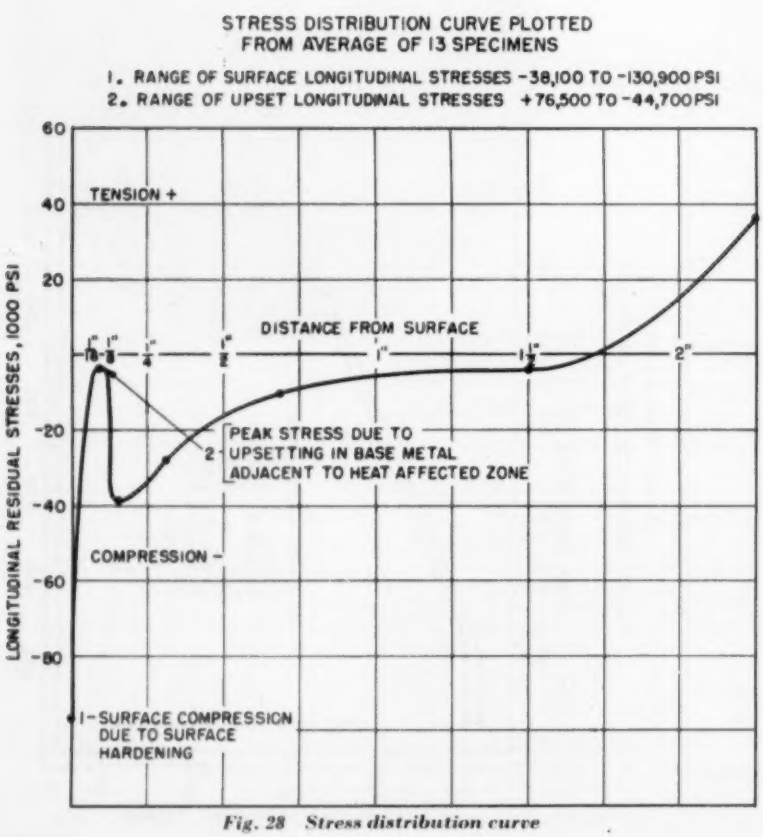


Fig. 28 Stress distribution curve

ting speed. With the armor plates tested, the cooling rates of armor plate cut at room temperature are sufficiently in excess of the critical cooling rate to induce full hardening in the heat-affected zone (neglecting possible small amounts of retained austenite) regardless of cutting speed or gas flow with acetylene, hydrogen or propane.

From the results obtained on oxygen cuts in rolled armor, the following conclusions may be drawn that should be equally applicable to any oxygen cut on alloy steel of similar hardenability.

1. *Fusion Zone of Melted Metal.* The metal at and immediately below the surface of an oxygen cut in armor plate has been melted during the progress of the cut and subsequently solidified.

2. *Composition of Fusion Zone.* Laboratory examinations (and literature references) indicate that the chemical analysis of this Fusion Zone will show higher carbon and nickel contents than the normal armor plate regardless of preheat fuel gas.

3. *Visible Heat-Affected Zone.* The boundaries of the Hardened Base Metal Zone in oxygen-cut armor plate extend from the Fusion Line to a section where the Ac_1 or lower critical temperature on heating has just been reached.

4. *Extent of Heat Effects Outside Visible Heat-Affected Zone.* There is a section adjacent to the heat-affected zones in heat-treated armor plate where temperatures below the lower critical but above a previous draw temperature have been reached and the plate annealed.

5. *Low Temperature.* The maximum hardnesses developed in the heat-affected zones of the oxygen-cut base metal are comparable, in the case of armor plate at normal or subnormal temperatures, to the hardnesses developed by water quenching from above the critical temperatures.

6. *Preheat.* When preheated plate is cut, the maximum hardnesses developed in the heat-affected zones of the oxygen-cut base metal are comparable to the hardnesses developed by water quenching from above the critical temperatures and drawing the material at temperatures equal to the preheat temperatures.

7. *Preheat.* At preheat temperatures of 600° F, the maximum hardness in the base metal can be lowered measurably by using low cutting speeds and high preheat fuel gas flows.

8. *Maximum Hardness.* The maximum hardnesses developed by oxygen-cut armor plate are located in the Fusion Zone, the layer of melted metal at the surface of the cut. These hardnesses are 50% higher than in hardened base metal zone because of higher carbon and alloy content.

9. *Preheat Fuel Gas.* No noticeable metallurgical effect results from changing the type of preheat fuel gas.

10. *Residual Stress.* Longitudinal compression stresses were found on the surfaces of all oxygen-cut edges of heat-treated armor plate cut at normal and subnormal temperatures and then tested.

Inert Arc Welding of 90–10 Cupro-Nickel

The development of a filler metal and welding procedures for producing sound satisfactory welds in 90–10 cupro-nickel by the gas-shielded inert arc process

by Lowell H. Hawthorne

INTRODUCTION

THE copper-nickel alloys of the 70Cu – 30Ni variety have long been recognized for their resistance to corrosion and erosion-corrosion. They have been used extensively by the Navy on all major combat vessels for sea water application since 1936. They have also been used quite extensively for commercial refrigeration condensers and the like.

During the last few years a great deal of work has been done to develop lower nickel alloys having good erosion-corrosion resistance. Extensive studies by the British Non-ferrous Metals Assn. followed by similar tests in this country indicated that a 90Cu – 10Ni alloy containing about 1.5% Fe was a very promising alternate for 70Cu – 30Ni alloy.

This alloy was first produced by Revere in the form of a condenser tube in December of 1947. However, until the current defense program gave rise to a critical shortage of nickel, its use was largely for condenser and heat-exchanger tubes. As a result, little was known about the weldability of this material for use in welded fabrication and construction. Some experimental welding of the 90–10 alloy was done in the Laboratory in 1949. Electrodes of the 70–30 alloy were used for this welding and the work indicated that satisfactory mechanical properties could be obtained in this manner. However, at the present time no electrode or filler metal of similar composition is commercially available in this country for welding the 90–10 alloy.

Recently, due to the critical shortage of nickel, the 90–10 alloy has been approved by the Navy and incorporated in the specifications covering tube, bars, plates, rods, sheet and strip (see Specification MIL-T-16420A, MIL-T-15005 and MIL-C-17526 as amended Aug. 1, 1951), to be used whenever possible as a substitute for the 70–30 alloy. The National Production Authority has also required the substitution of the 90–10 alloy for 70–30 cupro-nickel whenever possible for civilian production.

Lowell Hawthorne is connected with the Research Department of the Revere Copper and Brass Co., Inc., Rome, N. Y.

Presented at the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Feb. 15, 1953.

PROCEDURE

Materials

Plate material in thicknesses of 1/8, 1/4, 1/2 and 3/4 in. was obtained for this investigation.

Table 1 shows the chemical requirements of the aforementioned Navy specifications and the chemical analysis of the material used in this investigation. Material of this analysis was used for the entire series of welds included in this report. Early in the investigation some test welds were made in plate which contained 0.35% Mn because it was believed by some of the people involved in the development of this alloy that the higher percentage of manganese would be beneficial. This work was discontinued because the results obtained from these welds indicated that this was questionable.

Table 1—Chemical Requirements of Navy Specifications Covering 90–10 Alloy and the Analysis of the Material Received at the Laboratory for Use in This Investigation

Element, %	*Navy specification*	*Base metal*
Cu	*	88.49
Ni	9.0–11.0	Bal.
Fe	1.00–1.75	1.20
Mn	0.75 max.	0.13
Si		0.00
Pb	0.05 max.	0.008
Zn	1.0 max.	0.03
Al		0.001
Mg		0.05
S		0.006

* Copper + sum of named elements 99.50% min.

Table 2 gives the analysis of three filler wires, A, B and C. A and B are essentially the same as the plate materials. For the reason previously mentioned B shows a higher Mn content than A. Alloy C is the wire finally developed which gave consistently satisfactory results.

Table 2—Chemical Analysis of the Original 90–10 Wire Used in Experimental Welds and That of Wire Developed During This Investigation

Element, %	*A, Low Mn wire*	*B, High Mn wire*	*C, Wire as modified*
Cu	87.97	87.84	87.58
Ni	Bal.	Bal.	Bal.
Fe	1.55	1.46	1.54
Mn	0.22	0.39	0.17
Si	0.013	0.011	0.12
Ti	...	...	0.29
Pb	0.008	0.002	0.002
Zn	0.00	0.00	0.00
Al	0.001	0.001	<0.001
Mg	0.01	0.00	0.00
S	0.004	0.003	0.002

Table 3 gives the analyses of some of the compositions of filler metal used in the form of strip. One or two other filler metals were used and are mentioned briefly in welding procedures.

Table 3—Analyses of 90–10 Modified with Ti and/or Si for Experimental Welding. These Filler Metals Were Rolled and Cut into 1/8-In. Square Strips and Used Only with the Gas-Shielded Tungsten Arc.

Element	*Heat Nos.*		
%	*844*	*859*	*860*
Cu	Bal.	Bal.	Bal.
Ni	9.5	10.0	10.0
Fe	2.0	1.6	1.8
Mn	0.27	0.2	0.2
Si	..	1.0	1.0
Mg	...	..	..
Ti	1.0	..	1.3

Equipment

A 600-amp Hobart motor generator set was used as a source of welding current for all experimental welding.

Esterline-Angus recording volt and ampere meters were used to record and measure welding currents and arc time.

A Model 2 Aircomatic gun was used for all inert-gas-shielded metal-arc welding.

An Airco Machine Heliweld Holder in

conjunction with an Airco Model No. 8 Radiagraph carriage was used for inert-gas-shielded tungsten-arc welding where critical control of procedures was necessary.

A Linde No. HW4 250-amp Heliarc torch was used for all manual gas-shielded tungsten-arc welding.

Argon was used exclusively as the shielding gas with a standard argon flowmeter calibrated in cubic feet per hour.

Welding

It was originally planned that this investigation would be concerned with gas-shielded metal arc only. Later it was decided to make some welds using the tungsten arc method.

An original series of 12-in. welds in $^1/_2$-in. plate was made to develop procedure details and operating technique of the gas-shielded metal-arc process as the Aircomatic equipment had been acquired only recently.

These welds were made using $^1/_{16}$-in. wire of the composition A shown in Table 2 using a 60-deg Vee with a $^1/_{16}$-in. root face and a $^1/_2$-in. root space. The plates were tack welded at each end and dogged to the bench top against a $^3/_4$ x 2-in. grooved copper backer. The weld was deposited in three layers using a welding current of 275–300 amp with an arc voltage of 27–28. Argon was used as a shielding gas at a flow rate of 25 cfh. A record of welding speeds was not kept because these welds were not for test purposes.

It was immediately apparent that the welds were not of satisfactory quality due to porosity at or near the fusion line.

It was found that titanium, known to be useful in suppressing this type of porosity in other nickel-copper alloys, added to the weld by placing filings in the groove prior to the deposition of each layer, prevented or minimized this porosity. This method of addition was unsatisfactory, however, as the velocity of the shielding gas was sufficient to blow the filings ahead of the puddle and the titanium additions were not consistent.

Titanium hydride applied to the weld as an alcoholic paste and allowed to dry prior to the execution of each layer gave more consistent additions of titanium and was sufficient to prevent the formation of porosity. Chemical analysis of one of the welds made in this manner showed the presence of 0.16% titanium.

X-rays of a weld made in this manner and of one of the welds made without the addition of titanium are shown in Fig. 1, *D* and *E*.

As it was obvious that modification of the filler metal was going to be necessary and that the cost and time consumed in the production of $^1/_{16}$-in. diam filler wire of various chemical compositions was going to be prohibitive, it was decided that gas-shielded tungsten-arc welding should be included in this investigation. This permitted the use of $^1/_8$-in. square filler metal cut from strip material cast and rolled in the Laboratory. Moreover, by the use of the machine Heliwelder, it was possible to maintain rigid control over procedure details so that variation in welding procedures would not affect weld quality to any appreciable degree.

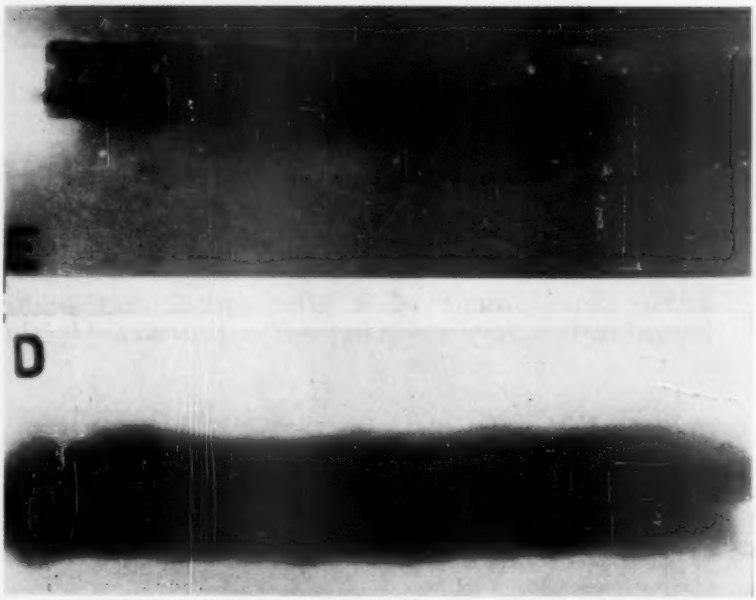


Fig. 1 (E) Radiograph of weld made with filler metal A, Table 1. (D) Radiograph of weld to which titanium has been added by applying alcoholic paste of titanium hydride prior to welding

Three such lots of filler metal were made and gas-shielded tungsten-arc welds in $^1/_2$-in. plate were made with each of these alloys. The analyses of the experimental lots of filler metal cast in the Laboratory are shown in Table 3. Welds were also made using filler metal A and B as shown in Table 2.

The procedure details used in the execution of these welds are as follows:

Base metal thickness, in.	$^1/_2$
Joint design	Square butt
Backing	$^3/_4$ x 2-in. grooved copper
Welding current, amp.	320–330
Arc voltage	14
Welding speed, ipm	16
Gas flow (argon), cfh	25

The mechanical test results according to filler metal identification are shown in Table 5, Series A.

It was apparent from the results of these welds that alloys containing a nominal 1% Ti or 1% Ti plus 1% Si were superior and that alloys containing the higher percentage of manganese or 1% Si alone were of inferior quality when compared to those obtained using the low manganese wire A in Table 2.

On the basis of these results, two lots of $^1/_{16}$-in. diam wire were obtained through the cooperation of International Nickel Co. for the Aircomatic equipment. One of these was modified by a nominal addition of 1% Ti and the other by 1% Ti plus 0.5% Si. The amount of silicon was reduced from 1% because it was feared that larger amounts of silicon might make the alloy hot-short.

Welds made with these alloys were completely unsatisfactory due to the formation of transverse shrinkage cracks. Longitudinal hot-short cracking developed in welds made with the alloy containing sili-

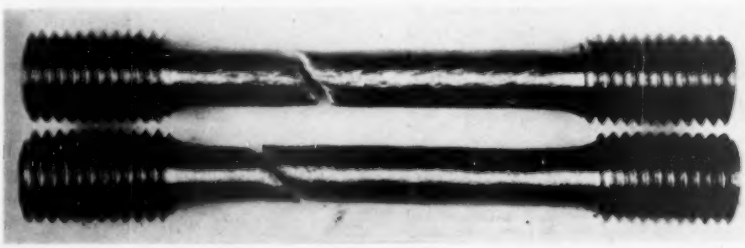


Fig. 2 Threaded end all-weld-metal specimens taken from gas-shielded metal-arc welds in $^3/_4$-in. plate

Table 4—Summary of Procedure Details for Gas-Shielded Arc and Gas-Shielded Metal-Arc Welds in Various Plate Thicknesses

Method	*Gas-shielded tungsten arc**		*Gas-shielded metal arc*†			
Plate thickness, in. →	1/8	1/4	1/8	1/4	1/2	3/4
Joint design	Square butt	60-deg Vee, no root face, 1/8-in. root space	Square butt	60-deg Vee, no root face, 1/8-in. root space	60-deg Vee, no root face, 1/8-in. root space	60-deg Vee, no root face, 1/8-in. root space
Backing	3/4-in. x 2-in. grooved copper backer strip	3/4-in. x 2-in. grooved copper backer strip	3/4-in. x 2-in. grooved copper backer strip	3/4-in. x 2-in. grooved copper backer strip	3/4-in. x 2-in. grooved copper backer strip	3/4-in. x 2-in. grooved copper backer strip
Electrode diameter, in.	1/8	1/8	...	...	...	...
Wire size, in.	1/16	1/16	1/16	1/16	1/16	1/16
Arc amperage	260	260	290–300	290–300	290–300	290–300
Arc voltage	14	14	28–30	28–30	28–30	28–30
Number passes	1	2	1	2	3	4
Average welding speed (total), ipm	8	2‡	16	7.7	3.6	1.5

*, † Argon flow for gas-shielded tungsten-arc welding was 20 cfh and 25 cfh for gas-shielded metal-arc.
‡ Slow welding speed is attributed to difficulty of adding filler metal in 1/16-in. wire diameter rapidly enough to maintain normal welding speeds.

con when an attempt was made to overcome this difficulty by reducing the currents and welding speeds.

Experimental welds were made using alloy A, Table 2 as the welding wire and adding specified amounts of the alloys mentioned above. Analyses of these welds proved that titanium content as low as 0.09% was completely effective in eliminating porosity in the welds.

Finally two lots of 1/16-in. diam filler wire were obtained, one of which was of the analysis shown for Alloy C, Table 2, and one of which contained no silicon as it had not definitely been determined whether or not additions to silicon were desirable.

Tests made using these alloys showed that welds made with filler metals con-

Table 5—Summary of Mechanical Properties Obtained from the Various Series of Welds Discussed in the Text of This Report

Weld No.	*Metal thickness, in.*	*Filler rod*		*Tensile strength, psi*	*Face bends*		*Root bends*	
		Ident.	*Diam., in.*		*Angle of failure,* deg*	*Elongation, %*	*Angle of failure, deg*	*Elongation, %*
Series A—Automatic gas-shielded tungsten-arc welds using various filler metals								
GSA 77	1/8	Heat No. 859	1/8	33,600	36	7.9	OK	22.2
		Table 3		22,400	27	8.3	OK	16.7
GSA 73	1/8	High Mn	1/8	27,800	73	19.2	86	53.3
		Table 2		29,300	57	16.6	57	38.5
GSA 74	1/8	Low Mn	1/8	39,200	OK	48.3	†	...
		Table 2		37,400	†		OK	68.7
GSA 76	1/8	Heat No. 844	1/8	43,500	OK	41.4	OK	47.0
		Table 3		43,600	OK	46.5	OK	50.0
GSA 78	..	Heat No. 860	1/8	44,400‡	OK	34.6	OK	35.7
		Table 3		44,600‡	OK	33.3	†	
Series B—Final series of welds made by the gas-shielded tungsten-arc process								
GSA 87	1/8	Alloy C	1/16	41,300‡	OK	10.3	OK	17.7
		Table 2		40,700	OK	25	OK	21.4
GSA 89	1/8	Alloy C	1/16	39,800	OK	21.7	OK	23.5
		Table 2		41,300	OK	28.5	OK	17.7
GSA 88	1/4	Alloy C	1/16	45,000‡	OK	14.3	OK	27.8
		Table 2		46,300	OK	12.2	OK	31.6
GSA 90	1/4	Alloy C	1/16	44,500‡	OK	11.1	OK	27.0
		Table 2		45,500	OK	12.5	OK	20.0
Series C—Final series of welds made by the gas-shielded metal-arc (Aircomatic) process								
GSMA 38	1/8	Alloy C	1/16	40,000	OK	21.6	OK	13.8
		Table 2		41,200	OK	19.0	OK	21.6
GSMA 39	1/8	Alloy C	1/16	40,300	OK	25.0	OK	10.0
		Table 2		39,700	OK	9.0	OK	26.3
GSMA 36	1/4	Alloy C	1/16	46,700	OK	21.4	OK	25.0
		Table 2		43,800	OK	24.0	OK	43.5
GSMA 37	1/4	Alloy C	1/16	47,500‡	OK	20.8	OK	19.0
		Table 2		46,100	OK	23.6	OK	28.5
GSMA 33	1/2	Alloy C	1/16	48,300‡	OK	19.1	OK	25.0
		Table 2		48,300	OK	23.4	OK	8.3
GSMA 34	1/2	Alloy C	1/16	48,300‡	OK	20.3	OK	17.0
		Table 2		48,400‡	OK	11.1	OK	20.7
GSMA 40	3/4	Alloy C	1/16	47,200‡	OK	17.4	OK	6.2
		Table 2		46,400	OK	17.5	§	...
GSMA 44	3/4	Alloy C	1/16	47,200‡	OK	18.0	OK	19.5
		Table 2		47,000‡	OK	18.5	OK	19.5
GSMA 35	..	All-weld-metal specimens		49,700	..	19.0	..	...
				52,000	..	22.5	..	...

* Angle indicates degree of bend from the flat.
† Specimen bent off center line. Elongation, if any, was not measured.
‡ Fracture occurred in the base metal.
§ This bend specimen contained a very large oxide inclusion which opened up immediately when bending started.

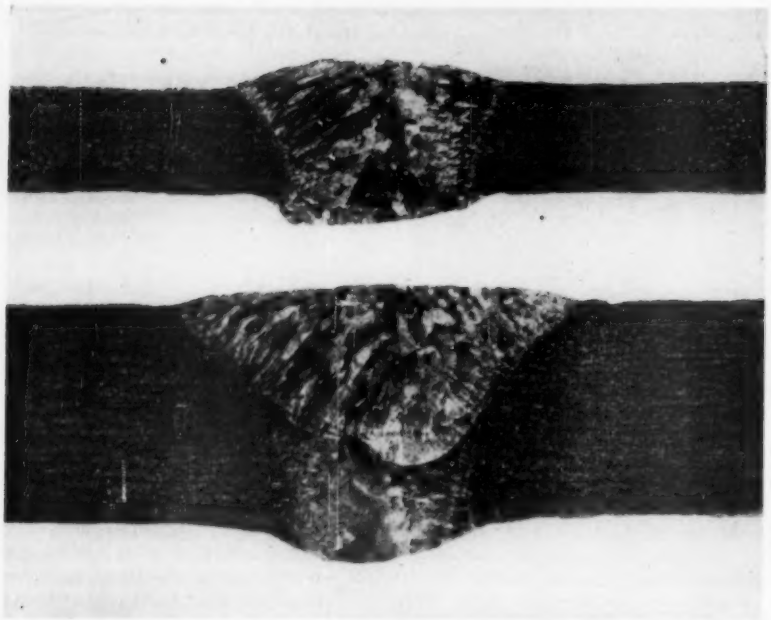


Fig. 3 - Cross sections of gas-shielded tungsten-arc welds GSA 87 and GSA 88. Note complete absence of any macroporosity. Etchant: HNO_3 + $HC_2H_3O_2$ + $FeCl_3$

taining equal amounts of titanium (0.29%) showed a definite increase in tensile strength where 0.12% silicon was present (Alloy C, Table 2). Average tensile strength for the welds made with the titanium-bearing alloy was 44,550 psi while those made with the alloy containing titanium plus silicon was 49,700 psi.

The final series of test welds included two gas-shielded tungsten-arc welds in 1/8- and 1/4-in. plate and two gas-shielded metal-arc welds in 1/8-, 1/4-, 1/2- and 3/4-in. material. All were made using Alloy C, Table 2.

The procedure details for these welds are shown in Table 4. All welds were allowed to cool to room temperature prior to the execution of subsequent layers.

The mechanical properties of these welds are shown in Table 5, Series B and C.

In addition a weld was made in 3/4-in. material to provide all weld metal test specimens (see Fig. 2). Procedure details for this weld were the same as given for welds in 3/4-in. plate above.

Mechanical Testing

All test plates were cut to provide two reduced-section-tensile, two face-bend and two root specimens.

Tests were conducted on 100,000-lb capacity Baldwin-Southwark hydraulic testing machine in conjunction with an Olsen Bending Test Tool.

The guided-bend tests were all made on a mandrel or roll whose diameter was four times the thickness of the specimen being tested.

Metallographic

Throughout the course of this investigation, metallographic sections were taken from all welds to determine the amount of porosity present but none will be included in this report except from the final series of welds. These are shown in Figs. 3 and 4. Figure 3 shows cross sections of gas-shielded tungsten-arc welds in 1/8- and 1/4-in. material. Those in Fig. 4 are from gas-shielded metal-arc welds in 1/8-, 1/2- and 3/4-in. material. All show complete absence of any macroporosity.

Figure 5 is a microsection at 50 diameters reduced one-third in reproduction from a weld in 1/2-in plate. At the right is clearly shown the dendritic structure of the weld and grain growth across the fusion line is apparent. Microporosity is obviously absent.

DISCUSSION

Comparison of procedure details, Table 4, and mechanical test results, Table 5, brought out some rather interesting points.

First, while there was considerable difference in the tensile strengths attained in welds in 1/8-in. plate and those attained in 1/4-in. 1/2- and 3/4-in. plate, the consistency of the values in the heavier plate is extremely good. This is especially true of the tensile specimens from two gas-shielded metal-arc welds in 1/2-in. plate where three specimens exhibited identical tensile strengths with failure occurring in the base metal in two of them. Figure 6 shows tensile and bend specimens from GSMA 36 from which tensile specimens showed identical tensile strengths of 48,300 with failure occurring in the base metal in one specimen and in the weld metal in the other.

The differences in tensile strength exhibited by the welds in 1/8-in. plate and plate of the greater thicknesses is probably due to the greater percentage of dilution with base metal which would inevitably occur due to differences in joint design. This would result in lowering the silicon content of the weld metal accompanied by a reduction in tensile strength. This conclusion was borne out, at least in part, by analysis of the weld metals from 1/8- and 3/4-in plate. These analyses are shown in Table 6.

Comparison of these analyses based on residual silicon indicates the dilution of filler metal with base metal is approximately 67% in 1/8-in. material whereas the dilution in the 3/4 in. is about 15%. There has apparently been a relatively higher loss of titanium and iron in both cases. The residual titanium content in each case was sufficient to completely suppress the formation of porosity and the loss of iron is not sufficient to be significant.

Analysis of the procedure details in Table 4 shows a very wide range of welding speeds for both gas-shielded tungsten-arc and gas-shielded metal-arc welding methods. Welds made with this filler metal did not show any evidence that hot shortness or any other consideration need be taken into account to produce sound welds. Production requirements would, of course, make high welding speeds desirable, but the uniformity of results at various welding speeds indicate that the need for special procedures would not be likely.

CONCLUSIONS

1. Filler metal of substantially the

Table 6—Chemical Analyses of Base Metal, Filler Metal and the Resultant Weld Metals in 1/8- and 3/4-in. Plates

Element %	*Base metal*	*Filler metal*	*Weld metal from weld in 1/8-in. plate*	*Weld metal from weld in 3/4-in. plate*
Cu	88.49	87.58	88.06	87.73
Ni	Bal.	Bal.	Bal.	Bal.
Fe	1.20	1.54	1.23	1.19
Mn	0.13	0.15	0.13	0.15
Si	0.00	0.12	0.04	0.10
Ti	0.00	0.29	0.08	0.18

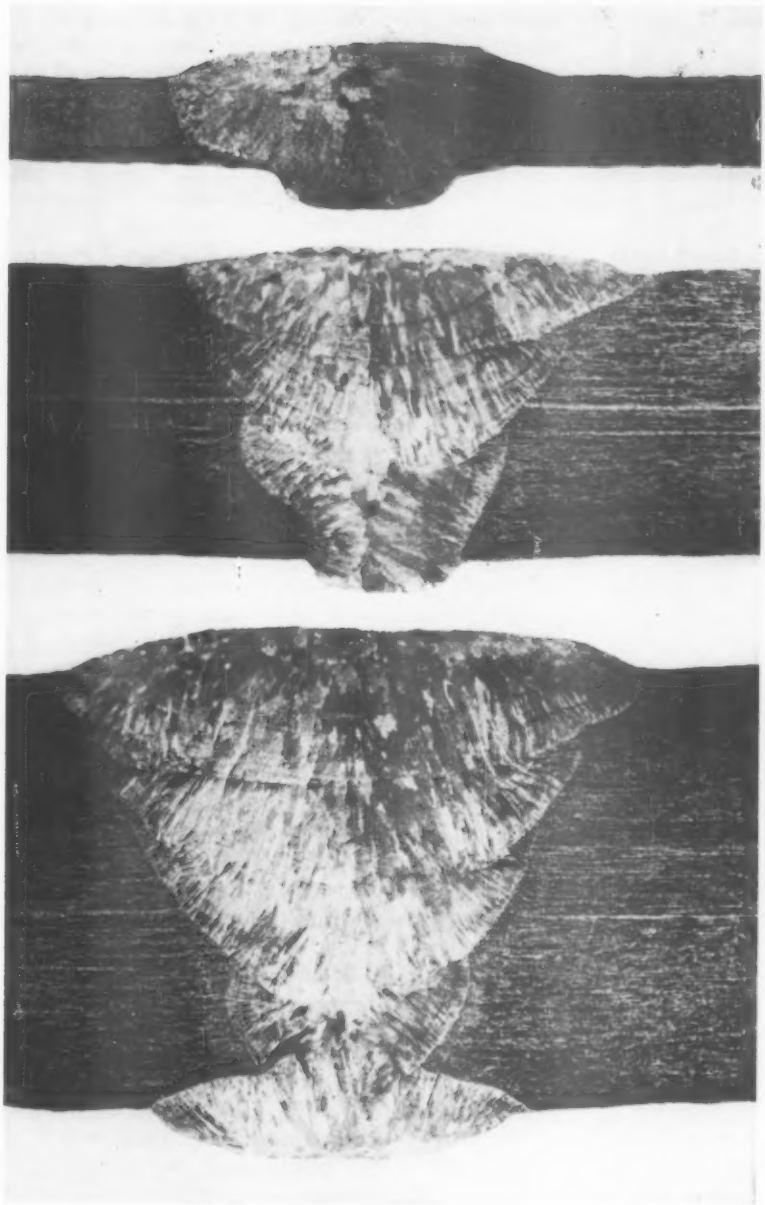


Fig. 4 Cross sections of gas-shielded metal-arc welds No. GSMA 38, GSMA 33 and GSMA 40. Complete absence of macroporosity is again apparent. Etchant: $HNO_3 + HC_2H_3O_2 + FeCl_3$

hot-short and special procedures are not believed necessary.

5. The equipment used in this investigation performed satisfactorily in all respects and was in no way modified for laboratory work.

ACKNOWLEDGMENT

The writer wishes to express his appreciation to Dr W. A. Mudge and the Technical Services Division of the International Nickel Co., and to G. W. Pirk and The Rome Cable Corp. for their assistance in the production of the various filler materials used in this investigation. Thanks are also due to the personnel of the Revere Research Laboratory for their cooperation in furnishing the photographs and the chemical and mechanical test data included in this report.

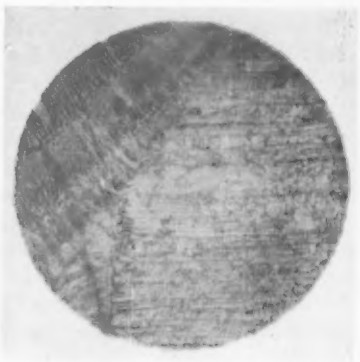


Fig. 5 Microsection taken from weld number GSMA 38. Dendritic structure of weld metal at left with grain growth across the fusion line is apparent. Dark inclusions are believed to be iron-rich constituent. Magnification 50×. Etchant: $HNO_3 + HC_2H_3O_2 + FeCl_3$

chemical composition shown in C, Table 2 will produce sound welds having good mechanical properties when used with either of the gas-shielded arc processes.

2. The effect of titanium is the suppression of fusion line porosity which is characteristic of gas-shielded arc welds in copper-nickel alloys.

3. The addition of silicon to the alloy containing titanium materially increases the tensile strength of the resulting weld.

4. This filler metal apparently is not

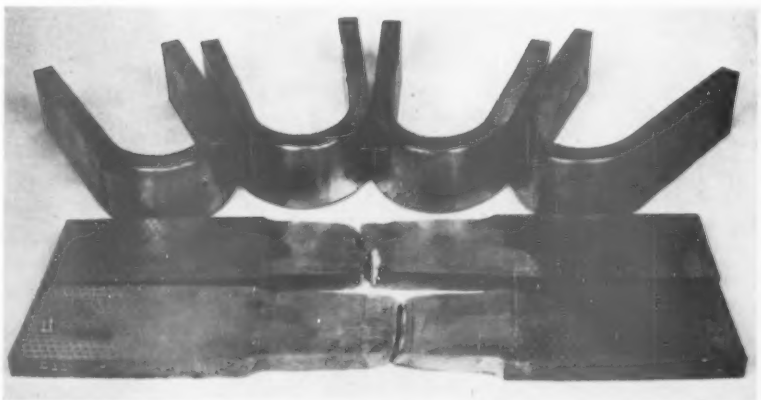


Fig. 6 Bend and tensile specimens GSMA 38. Tensile specimens failed at identical values of 48,300 (see Table 1) with failure occurring in the base metal in one case and in the weld metal in the other

Discussion by H. C. Cook

In our work on the Aircomatic welding of the various copper alloys, the weldability of both the 70–30 and 90–10 copper nickel was investigated. It soon became obvious that the standard 90–10 alloy composition left a great deal to be desired as an electrode material for this welding process. Although the welding arc behavior was satisfactory and the resulting beads appeared to be of good quality, X-ray radiographs revealed a large amount of macroporosity at the weld metal-base metal interface. Similar results were obtained with welds in the 70–30 composition except the porosity was lessened to the micro variety. The mechanical test results reflected the presence of the porosity with low ductility and poor impact values for material of this type.

I would like to compliment the author on the thoroughness of his investigation of effects of each of the elements which might possibly contribute to reducing the porosity in the weld. The question of increased silicon content has been outstanding with me since our use of a silicon containing 70–30 electrode material visibly reduced the porosity of the deposited metal.

It seems odd to me that in this investigation, which deals directly with weld soundness, there is a scarcity of radiographic data, the presence of which could, I am sure, strengthen the results. If the author can make some of his modified 90–10 wire available, I should like to have radiographs taken of welds made in our laboratory in order to round out the data on this material.

It would be of interest, I believe, if the author could give us his insight as to the possible mechanics of the effect titanium has on the deposited metal in preventing this fusion-line porosity. In the same vein, what possible effects might the remaining titanium have on the corrosion resistance of the welds? In other words, in what form and where does this residual titanium appear in the structure, and might its presence make this highly resistant material corrosion sensitive?

Poor weldability is always a serious stumbling block in the path of complete engineering utilization of a metal and alloy. In view of the possibilities of the 90–10 cupro-nickel for use with corrosive media, the results of this paper should be well received by all concerned.

H. C. Cook, Air Reduction Laboratory, Murray Hill, N. J.

Author's Reply

In reviewing Mr. Cook's discussion it should be pointed out that the presence or absence of porosity in the welds made during this investigation was determined by careful microscopic examination in all cases with the exception of the radiographs included in the paper. This was done because equipment for radiographic examination was not readily available and because even severe microporosity is difficult to detect and evaluate in radiographs.

Mr. Cook's remarks pertaining to the reduction of porosity in 70–30 cupro-nickel by the addition of silicon is seemingly in direct contradiction to the results obtained by the addition of silicon to the 90–10 copper-nickel alloy. In this investigation 1% of silicon added alone greatly increased the porosity in the weld with corresponding loss of physical properties shown in Table 5, weld No. GSA 77.

It is believed that titanium, present in the amounts shown in Alloy C, Table 2, is in solid solution.

The titanium possibly has two functions. First, it is an energetic deoxidant and combines with oxygen present and forms titanium dioxide which readily floats to the surface.[1] Second, it reduces the surface tension and increases the fluidity of the weld metal which assists in the evolution of gas or oxide.[2]

It is difficult to evaluate the effect of the small amounts of titanium and silicon, which were added to the filter metal, on the corrosion resistance of the material. Mr. Wilson Lynes of the Corrosion Section of the Research Department has pointed out that these additions might have a beneficial effect in some cases and an adverse effect in others. The only way to determine the extent of such effects would be by trial under actual conditions.

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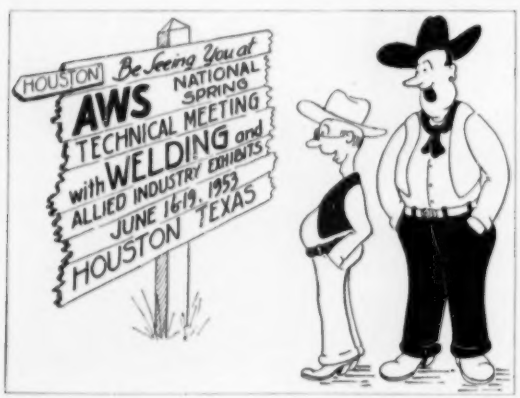

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Flame Hardening of Large Diameter Thin-Wall Cylindrical Shells

The adoption of the flame-hardening process to progressively "through harden" thinwall cylinders with a minimum distortion and necessary equipment

by Gustave A. Weber

SEVERAL years ago the company with which the writer is associated undertook a program for the development and limited production of extremely lightweight steel pressure vessels. Since these thin-wall vessels, to be made of economical materials, required special construction, it was necessary that a rapid heat-treating method be devised which would at once yield reproducible results within a narrow hardness range and have high production possibilities.

In the consideration of the heat treatment required, a review was made of the commonly available heating methods such as gas- and oil-fired furnaces, atmosphere controlled furnaces and salt baths in combination with various type quenches as well as quenching media.

It is not intended to review here the advantages and disadvantages of any of these processes. It is only necessary to note here that the flame-hardening process appeared to have sufficient potentialities to warrant its development on an automatic basis. Oxy-acetylene flames, used for many years for surface hardening, gave little distortion and clean surfaces. The adaptation of the flame-hardening process to progressively "through harden" thin-wall cylinders with a minimum of distortion will be discussed in this article along with a description of the machine in present use.

Initially, the cylinders, or shells, to be quench-hardened and tempered were 8 in. in diameter by 36 in. long. They were made of 0.046 in. sheet rolled into a cylinder, and butt welded along the longitudinal seam. Thicker rings, which were tapered to the same thickness as the shell, and which were also to be hardened, were butt welded to each end. The material of the shell and the end rings was 4130 steel. Full hardness was desired which, for this material, was Rockwell 53 to 55 on the C scale. The maximum out-of-round limitation was $^1/_{32}$ in., with the longitudinal straightness or bow also limited to $^1/_{32}$ in. Conventional tempering methods were to be used to obtain the required ductility with the high strength levels produced.

With these problems and requirements to be met, an exhaustive series of tests was carried out in the investigation of the progressive flame-hardening method. Flame hardening simply consists of progressively heating a narrow band of the metal around a rotating shell and following with a water quench to obtain full hardness. While the basic concept was simple, the ultimate operation required rather complicated manipulations. Heat was supplied by oxy-acetylene flames from burners that had a narrow range of operation; therefore, to control the heat in the shell, it became necessary to vary the speed of progression of the flames—faster if the shell were heated too rapidly, slower if the required temperature was not reached. The heating burners were placed in a single row around the shell since it was considered that if the heated band of metal were too wide, distortion in the shell would result. Because the calculated number of burners had to be spaced rather closely together, auxiliary air to complete the combustion of the oxy-acetylene flame had to be introduced from above or below the row of burners. Water was used for cooling the burners as well as in the quench. Other quenching media could not be considered because of the proximity of the flames.

With these factors entering into the design, the construction of the Increment Heat Treat Machine, as it was called, was begun on a development basis. The machine (Fig. 1) consisted of a four-post frame with cross members at the top and a vertical spindle in the center. The spindle, driven by a separate motor from the top to avoid the quenching water, was in two parts, the major portion of which was a removable shaft to support and turn the shell being hardened. The lower end of this shaft rotated upon an ordinary lathe center mounted in a fixed post. A moving platform or table with a circular hole in the center was suspended by counterbalanced chains and guided between the upright posts. This table carried all of the equipment which accomplished the actual hardening of the shell. On the table (Fig. 2) were the following:

Gustave A. Weber formerly with the Special Projects Department of the M. W. Kellogg Co., Jersey City, N. J. and currently chief Product Engineer, Gustav-Glaser Co., Patterson, N. J.

Presented at the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Feb. 15, 1953.

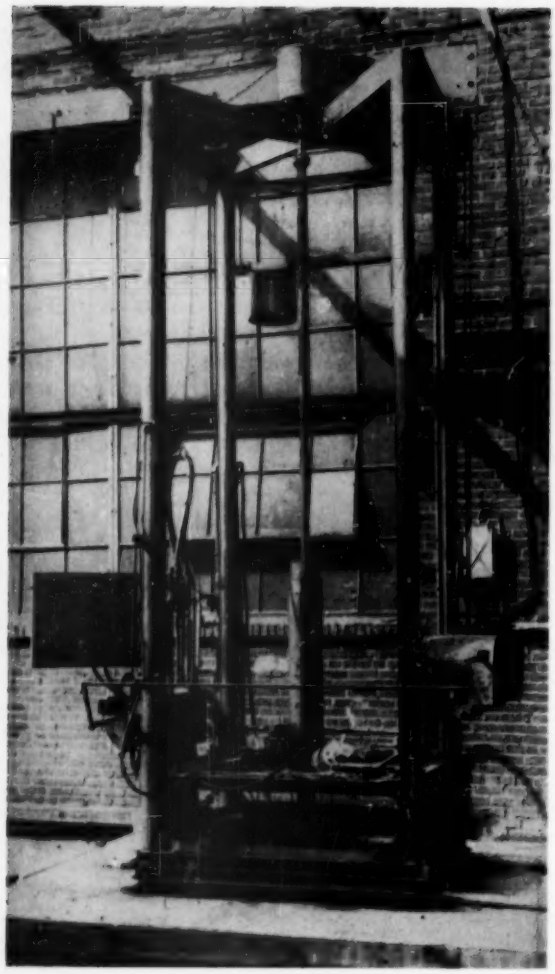


Figure 1

(*a*) A single circular row of seven tip flame heads of commercial manufacture.
(*b*) A water-quench ring.
(*c*) An auxiliary air ring.
(*d*) Blow pipes to mix the oxygen and acetylene.
(*e*) Mixed gas distributors.
(*f*) Water manifolds to distribute cooling water to the flame heads.
(*g*) Tubing, hoses, fittings, etc., to carry oxygen, acetylene, mixed oxy-acetylene gas, air, water and city gas.
(*h*) Pilot lights.
(*i*) Radiation pyrometer.

The radiation pyrometer was connected to a recording potentiometer from which the operator could determine the temperature of the shell and thus regulate the speed of the table. The chains moving the table were driven by a motor through a variable speed drive and control was hand operated. This hand operation was quite erratic, therefore an automatic control was devised with electronic circuits. This automatic modification utilized "proportioned plus" lead control in conjunction with a thymotrol system and a series wound motor. The radiation pyrometer picked up temperature signals from the heated area and transmitted them through the recording potentiometer to the thymotrol system, which regulated the speed of the motor driving the table. Thus the speed of progression of the flames could be regulated to produce a predetermined temperature range. The flame heads burned a mixed gas—oxygen and acetylene—in approximately equal proportions. There were seven tips on each head which provided up to seven separate flames. The gases were mixed in two blowpipes mounted on the table and conducted through distributors and copper tubing to the flame heads. All controls (Fig. 3) for

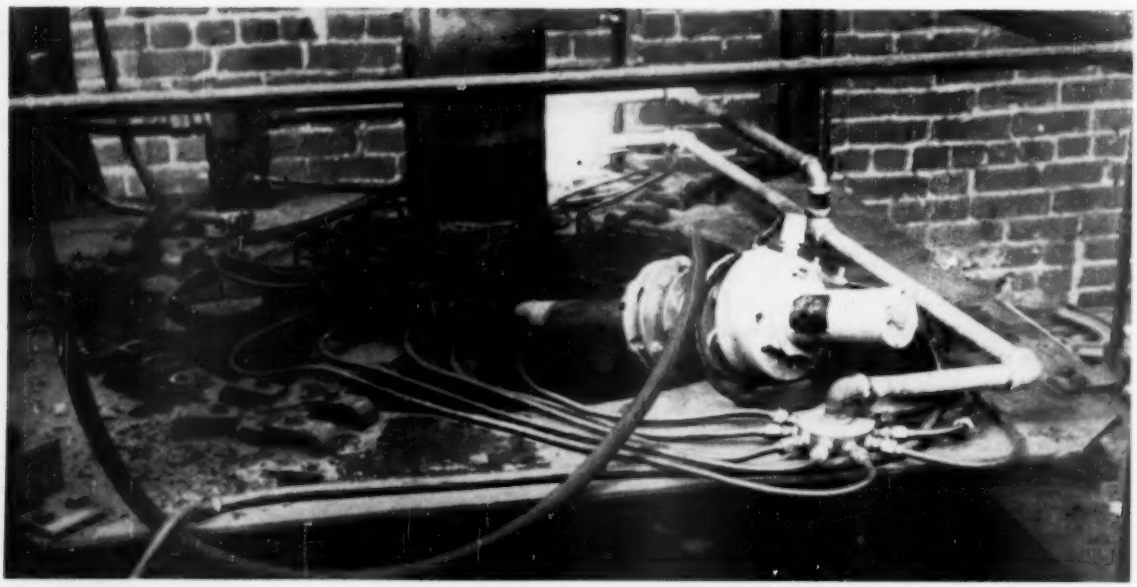


Figure 2

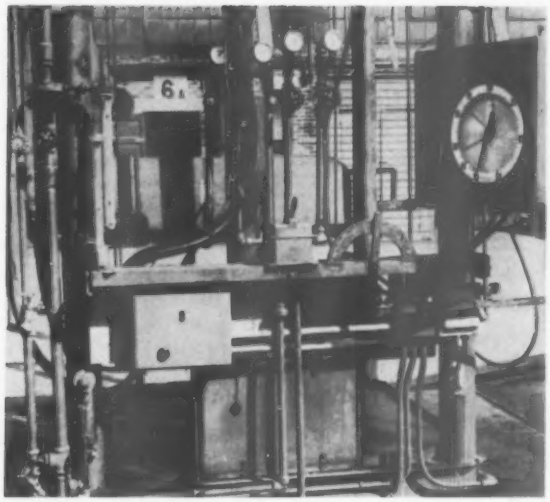


Figure 3

cooling water, quench water, oxygen, acetylene, air and city gas were mounted on a panel immediately to one side of the machine.

The cylinder or shell to be hardened was mounted upon spiders on the long shaft of the spindle (Fig. 4) and rotated at 70 rpm. The speed of revolution was not found to be critical although too low a speed on the smaller diameters would be likely to cause hot spots while too high a speed on the larger diameters could cause excessive vibrations and flame deflection.

During the investigation and development period, changes in requirements were made to include shells of larger size to be heat treated in the same manner. The diameters increased progressively in several stages to over 30 in. while the length increased to over 106 in.

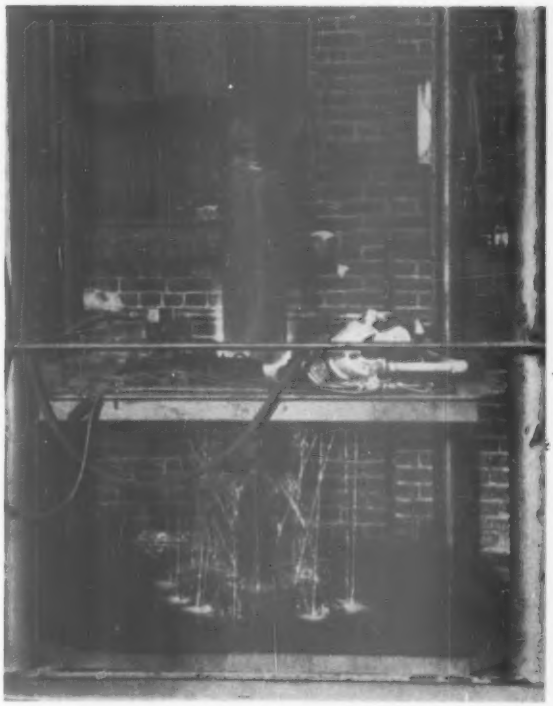


Figure 4

The wall thicknesses of these larger shells were generally kept in proportion to the diameters. In the larger diameters, however, 0.250 and 0.125 in. thicknesses as well as the 0.187 in. thickness occurred. Therefore, it was necessary to build a machine of larger capacity.

In the original development machine, it was necessary to construct a separate burner table for each diameter since it was impossible to move the flame heads (Fig. 5). It was also found that with the single row

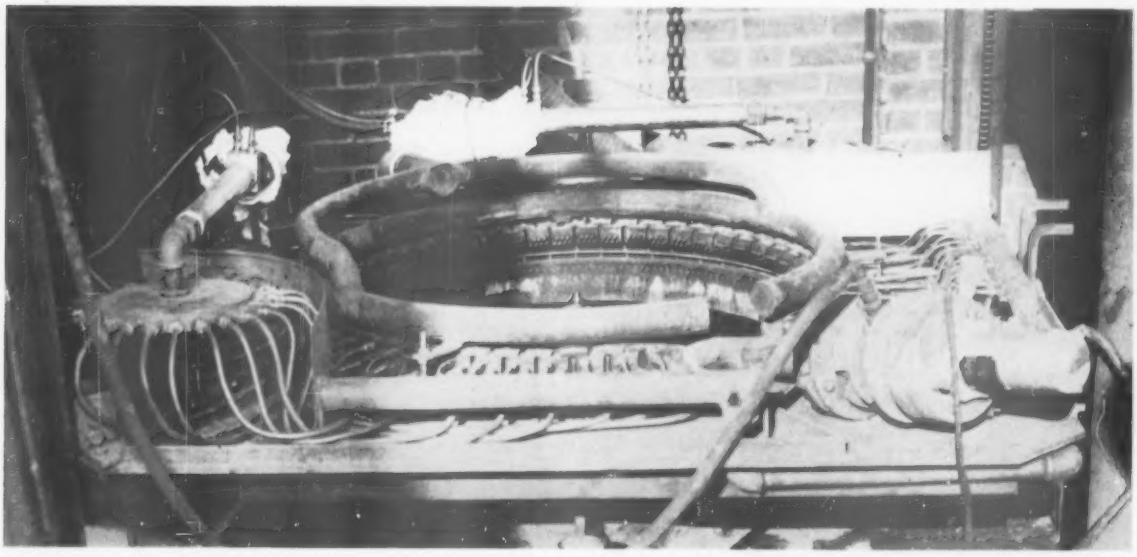


Figure 5

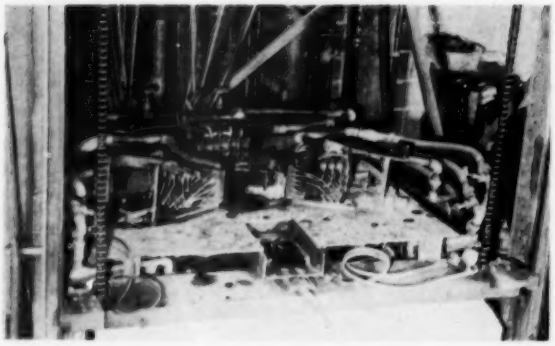


Figure 6

of flame heads, too concentrated a heat distribution occurred to permit full heat penetration without overheating the surface of the shells with larger diameter and thicker walls. Therefore, the design of the larger machine was based on a bank type burner setup—several rows of flame heads set one above the other (Fig. 6).

While it would appear that the multiple rows of flame heads would widen the red heated band of metal, this was not the case. The first row of burners heated the shell to temperatures between 500 and 600° F, the second row to approximately 1000° F (still black heat) and the third row raised the temperature to 1600° F.

With this configuration, the heads were grouped in banks, with fewer flame heads in each row. The circular arc length of the banks, however, was kept relatively short. All components, flame heads, mixed gas distributors, cooling water distributors, quench and auxiliary air nozzles were mounted on slides with flexible connections so that the complete bank of burners could be moved in or out as a unit. This configuration possessed two main advantages: a single table could be used over a range of sizes and the flame heads could be set at an optimum distance from the shell to be heated. The range of sizes was set at 6-in. diam increments since greater increments would cause the arc length differentials to be too great, which would result in the placement of the center flame heads or the end flame heads too close or too far away from the shell according to whether a larger or smaller shell diameter was hardened. With three tables, a range of diameters from 13 to 32 in. could be hardened. Another advantage of this bank-type burner arrangement was that the supply tubes from the distributors could be of relatively uniform length to eliminate starving tendencies on those heads which otherwise would have long connecting tubes. This was also true of the quench and auxiliary air rings. A further advantage in using the bank-type units came in heating the extra thin walls of some of the larger sizes of shells. Since the flame heads had to be operated within a narrow gas flow range, one or more banks could be left inoperative while the others did the heating thus eliminating difficulties from low gas pressure operation.

Another considerable operating improvement was in the method and principle of supplying additional air to complete the combustion of the oxygen-acetylene flame. The flame heads could be set slightly apart to permit auxiliary air tubes to be inserted between them and thus effect better distribution of the air. An air-to-mixed-gas ratio of $7^1/_2$ to 1 was required for the flames. However, when this ratio was approached the air had a marked tendency to cool the shell and splash the flame considerably. With an oxygen bleed into this air line, operation with considerably less volume of air and better flame characteristics was obtained, especially when preheat burners were used in conjunction with the bank burners.

Preheat burners set both on the moving table and on separate supports could provide additional operational advantages in heating or preheating thicker sections attached to the shell. With thick and thin sections occurring in a shell, a longer or greater application of heat to the thicker sections is necessary. Since quenching rates affect hardness, it was necessary to maintain a fairly constant quenching speed over a given thickness of shell wall. Because the burners traveled at the same speed and were set somewhat ahead of the quench ring, it was necessary to either separately preheat a thicker section or to turn on additional heat, and sometimes both, in order to keep the table speed within the proper quenching range.

All burner banks, preheat burners, pilot lights, etc., were operated by individual controls (Fig. 7) which required separate hose connections from the control station to the table. The water and air supplies, however, were carried to the table in one large service with preset controls mounted directly on the table.

Power for turning the shell was delivered, through a variable speed control, from below since it was found that the large volume of rising hot gases presented difficulties. This drive also simplified the transfer of the shell units into and out of the machine and other operations.

The operating characteristics of the Increment Heat Treating Machine depend in some measure upon the alertness and skill of the operator. By varying acetylene and oxygen pressures he can achieve optimum heating conditions. However, the actual temperature of the heated shell is kept constant by the automatic speed

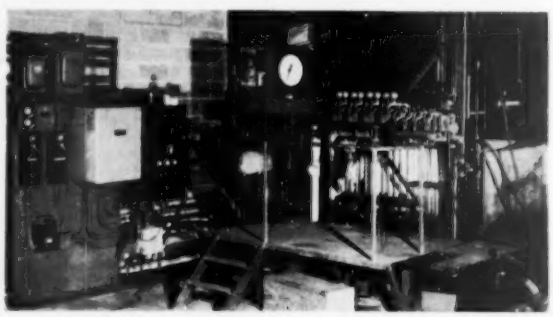


Figure 7

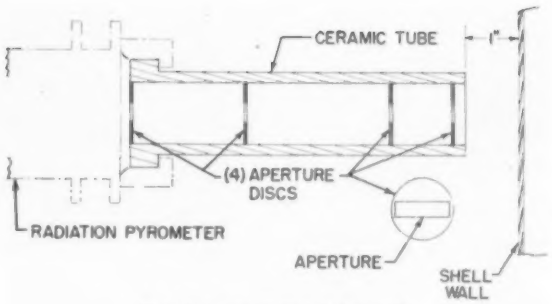


Fig. 8 Pyrometer sight tube

control of the table. The radiation pyrometer is equipped with a focusing tube (Fig. 8) with a narrow rectangular aperature set horizontally. The aperature narrows the recorded field in the direction of travel; however the aperature must not be too narrow since it also limits the radiation passing through to the thermocouples. This tube is focused immediately below the bottom row of burner tips at a point on the forward slope of the "hot spot" zone of the metal. It is characteristic that heat input reflecting from the back or inside face of the metal plus burning of gas by the auxiliary air should heat the metal surface just following the flames to a slightly higher temperature which immediately levels off again so that the heated metal stays at a fairly constant temperature until the quench reaches it (Fig. 9). The tube is focused on the forward (that is, direction of travel) slope, instead of the hottest point, for better control. If the tube is focused at the hottest point and the metal temperature becomes too high, the table speed would increase, but because the focal point of the pyrometer is traveling over an overheated zone, the pyrometer signals will continue to cause higher and higher table speeds until the flames are traveling too fast to heat the metal and before the momentum of the table can be overcome an overriding of the heated zone with a consequent cooler band on the shell occurs. This hunting effect can be controlled by special apparatus but it is easier to control by proper focusing of the radiation pyrometer tube.

Operating pressures for the oxygen and acetylene vary according to the number of tips in operation on each bank. Thick-walled shells require more tips and

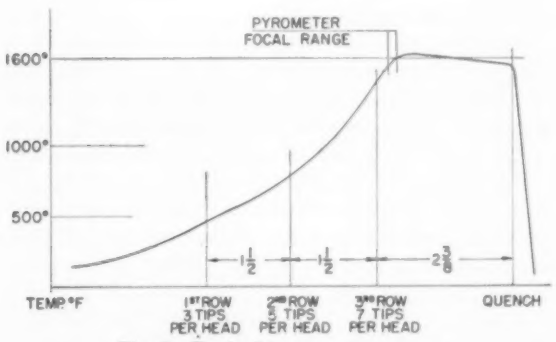


Fig. 9 Typical temperature curve

possibly additional banks. In the design of the machine, the number of banks and thus the number of operating tips were set for maximum anticipated wall thickness for the particular diametral range. The blowpipe or mixing torch for each bank has a capacity of 1000 cu ft of acetylene and oxygen per hour, but could be operated at volumes as low as 500 or 600 cfh. However, perhaps because of the distribution, the lower range is extremely sensitive to pops, blowbacks and flashbacks; therefore operation in this range is avoided.

A number of checks made with the bank-type burners, showed that a volume of approximately 14 to 15 cu ft of acetylene is required to heat 1 cu in. of steel per minute to 1600° F. With this volume and a given shell diameter and wall thickness, the table speed and number of operating tips may be determined. The tips have a No. 56 drill hole and a capacity of 10 cfh. However, the optimum operating range is 8 to 8 ½ cfh per tip. For example, the 31-in. diam shell with a ¼-in. wall has a cross-sectional area of 24 sq in. of steel. To heat this shell, 24 x 15 cu ft or 360 cfh at a table speed of 1 ipm is required. Table speed, however, must be fast enough to properly quench out the metal. In this case the speed is 12 to 13 ipm; therefore 4600 cu ft of acetylene per hour is required. Since the burner tips operate at 8 cu ft and a total of 504 tips in this setup provide a capacity of only 4000 cu ft, the heat of the bank-type burner must be supplemented with two special 39 tip burner heads which provide a total of 600 cfh additional capacity.

The auxiliary air to complete the combustion of this large volume of gas is supplied in volume at 2 to 3 lb pressure. It is enriched by bleeding 10 to 15% of oxygen into the air line to reduce the total volume of air required. Caution must be exercised, however, since air mixtures possibly containing oil vapors become dangerous at 50% oxygen content.

The operator presets the auxiliary air for each burner bank in use, making sure that uniform and correct volumes of air are distributed. These controls are mounted on the burner table. The air should be moisture free, which is accomplished by an air filter in the main supply line adjacent to the machine.

The water for the cooling of the flame heads is fed through separate controls which are preset for each operating condition. It drains directly into the tank at the bottom. The quench water is fed through rectangular tubes, the flat sides of which have four to five rows of holes. Thus the lead stream of water, which has a tendency to bounce away from the hot metal, is followed by successive streams which cool the metal. At first it was attempted to meter the quench water but a sudden line pressure drop caused a failure in the heat treating of a unit; ample water is now supplied through a quick-acting lever valve which may be very rapidly adjusted during the heating cycles if necessary.

From 450 to 500 gpm of water is used to quench the 31 in. diam by ¼-in. wall shell and 150 to 175 gpm for

the 16 x .100-in. wall shell. While this is in excess of the amount actually needed, it is felt that the small added water cost is more than justified. The larger volume of water also checked the formation of steam.

Considerable difficulty was encountered with the burners mounted in a single row completely around the shell (Fig. 5). It was necessary to remove and clean the burner tips after nearly every heating run, possibly because the flame heads which held the tips, being set too close together, were not properly cooled. In addition, when the auxilary air was introduced from below, there was a tendency for the air, because of its force, to suck water vapor up from the quench and thus form steam in the heated area and cause frequent pops. An attempt was made to use air from above but this pushed flames down and under the table and caused overheating of the tubing which supplied mixed gas to the burners.

With use of the bank-type burners, however, very little difficulty is encountered in keeping the tip orifices clean. However, because of the large acetylene capacities involved, dirt and foreign particles in any new installations having many manifolds, pipes, hoses, distributors, tubes and flame heads are likely to be present and before attempts are made to operate the machine (Fig. 10) when it is new all foreign matter must be carefully removed. At the start of each day the burners are test lit, but little or no tip cleaning is now necessary.

A precautionary measure should be taken to prevent any accumulation of acetylene and oxygen gas during blowdowns of the flame heads. A large pipe with a row of holes extends across the inside frame over the quench tank and before the burners are blown down a blast of air is turned on to dissipate the acetylene. This is shut off again before actual operation.

A pilot light, operated with city gas, is placed at each burner, with a separate shutoff at each hose connection; and a master valve is provided so that all pilot lights may be shut off by one control. A long pipe, attached to a gas hose, is kept lit and readily available in case blowbacks should blow out a pilot light.

A spray curtain is made to follow the table up a short distance to prevent splash of water over the floor.

The main power drive spindle for rotating the shells and the upper idling spindle have standardized adapters to accommodate locators and holding rings of various sizes. To facilitate loading and unloading the upper spindle is retractable; the lower drive member has a positive clutch which can be disengaged to permit turning the drive spindle by hand. Other safety facilities include a manually operated rheostat to raise or lower the table as well as a hand turning lever to accomplish the same function in case of power failure. CO_2 extinguishers and fire hoses are located conveniently in case of fire.

Loading and unloading is accomplished by means of rope slings which can be quickly placed around the shells. Use of rope is preferred to avoid scratches which may induce hardening cracks. The shells are hoisted

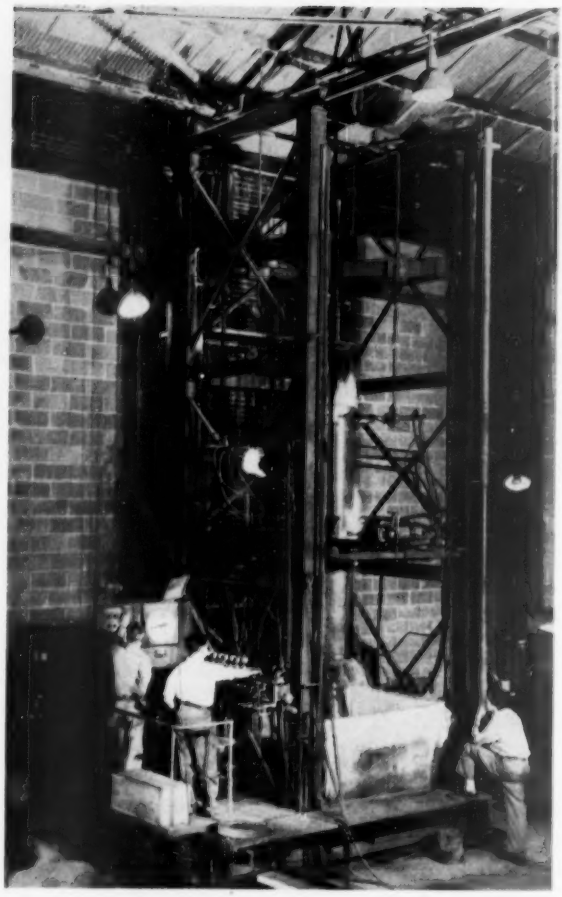


Figure 10

and carried into the machine from an overhead monorail. At first a mechanical swing arm with clamps to hold the shell in a vertical position was used but was found to be too cumbersome. Extreme care must be used in removing the hardened shells from the machine and in transporting them to the tempering ovens.

While 100% full martensite transformation is desired, it is not achieved. A hardness of Rockwell C50 to 52, better than 90% fully hardened, is obtained. By keeping the tempered steel on the high side of the required range, no difficulty is encountered with the shells in subsequent pressure tests which are made to prove shell strengths.

Out of roundness is consistently less than $^{3}/_{64}$ in. in the 16 in. diam size and $^{3}/_{32}$ in. in the 31 in. diam size. Straightness is satisfactory, with variations usually not exceeding $^{1}/_{64}$ in. on either side of the long axis; bow is less than $^{1}/_{32}$ in. for all sizes. Careful records are kept on all production shells as well as on experimental runs. The temperature recorder charts are marked with pertinent information relative to the unit being hardened and a diagram on the reversed side indicates growth, hardness, etc.

A peculiar phenomenon occurs with this method of heat treatment. A uniform diametral growth of ap-

proximately 1/2% of the diameter occurs with a corresponding shortening of the shell. This growth occurred with all the shells of different sizes processed in this manner. This phenomenon was utilized in enlarging a number of 16-in. cylinders with 1/4-in. walls that were 1/16 in. too small in diameter. Normally, to compensate for this growth the shells are fabricated 1/2% smaller.

Time required for increment heat treatment depends upon the amount of preheat that is needed on the heavier end sections as well as upon the length of the shells to be hardened. Of course the larger and heavier units, since they are somewhat cumbersome, require more time for handling. The 16 in. diam units, weighing 500 lb, are heat treated at the rate of 4 to 5 per 8-hr shift while the 31-in. units, weighing 1400 lb, are treated at the rate of 3 to 4 hr each

CONCLUSION

While at first the development of the increment heat-treating machine caused many hours of trial and trouble, yet promises of good results encouraged efforts until the process was finally ironed out in the building of the production machine. All results, good, bad or indifferent, reflected in the improvement in the design and the operation of this new machine. Further development in improved operation such as flashback-proof burners, growth control and tempering are contemplated.

Experiments have shown that tempering can be accomplished rapidly. However, so many factors, are involved that considerable investigation and development work must be carried out before a foolproof process can be developed for a production operator.

ACKNOWLEDGMENT

The author wishes to express his gratitude to Sherman Wing, formerly associated with him in the company, who collaborated in much of the development work, for his suggestions during the preparation of this paper, and to Harry S. Blumberg for his kind assistance.

Steel Strengthened with Aluminum Nitride*

by K. A. Krekeler

A NEW high yield strength steel was used in the construction of the roadway of the new bridge across the Rhein. The roadway consists of a plate stiffened underneath with longitudinal and transverse beams. A zigzag of flat steel bars is tacked to the upper surface of the plate in which a new type of asphalt is poured. The new steel is called ST50 MES and is very similar to the high-tensile steels used during the war, which were based on silicon and manganese without other alloys. To avoid difficulty in securing adequate yield strength, the new steel contains aluminum which combines with nitrogen to form aluminum nitride. The nitride forms a "critical dispersion" throughout the structure of the steel which raises its yield strength and yield ratio only, without affecting the critical cooling rate. Unlike steels containing carbide-forming elements, the new steel has no greater tendency toward martensite formation during welding than the same composition without aluminum.

The new steel contains 0.20 C, 0.45 Si, 1.00–1.20 Mn, 0.018–0.030% total Al and may be made in open-hearth, arc or induction furnaces. The aluminum nitride results from the reaction of the added aluminum with the nitrogen (0.006%) ordinarily present in the steel. The nitride makes the steel insensitive to aging and fine grained up to 1800° F. The steel is welded with the same electrodes used for low-alloy, high-yield steels. Covered electrodes, submerged arc and firecracker processes produce excellent butt and fillet welds. Bead-bend tests on plates 3/4 in. thick welded at 32, 14 and −4° F and bent at room temperature or −14° F gave 57 to 88° bend angle at the first cracks. All specimens bent 180° without complete failure. The maximum hardness in the heat-affected zone was only 200 Vickers.

Specimens of the steel with ribs welded to the surface similar to the bridge roadway were tested in pulsating tension fatigue. The fatigue limit (2 million cycles) was 29,000 psi, compared with 24,000 psi for ST52 not strengthened with aluminum nitride. The static tensile properties of the unwelded ST50 MES were: yield strength, 53,500 psi; tensile strength, 79,000 psi; elongation, 25% in 10 diameters. The corresponding values for the aluminum-free ST52 were: 42,500, 76,000 and 25.4%.

* Abstract of "Schweissversuche an Stahl mit einer Durch Submikroskopische Einlagerungen erhöhten Streckgrenze," published in *Stahl u. Eisen*, 72, 1278–1280 (1952). (Abstracted by Dr. G. E. Claussen.)

Wear and Operation Problems in Maintenance

Some recent examples of routine maintenance jobs in a large steel mill

by Frank J. Gaydos

IN THESE days of the narrowing of the spread between selling price and production costs, the machinery must be kept performing its prime function—production—as long as possible without shutdown for overhaul or breakdowns.

The maintenance man fits into this picture like the family doctor. Always on the job, ready to help by deed and advice, to solve minor ills and accidents, advise on major problems in the complex operations of present-day operations.

For the purpose of this paper, we have taken examples of routine work, occurring daily around the clock, seven days a week. The methods of doing these jobs change as better ideas and materials are found or developed. The examples illustrated are very recent.

Although our maintenance problems occur everywhere in the extensive area of the "Gary Steel Works" of the United States Steel Co., we feel that our approach to these problems is similar to that in other industries. We all need quick repair in breakdowns, prompt replacements in overhaul and downturns and we all have the everlasting combat problem—wear.

Each operating division of Gary Steel Works is assigned welders and burners to work with their "bullgangs" or spot repair crews. This personnel is entirely under the jurisdiction of these divisions. The Central Welding Department is a unit of the Maintenance Division, the same as all craft shops, i.e., mason, foundry, construction, etc., departments.

It is the function of the Central Weld Shop to furnish welding and burning service to all shops; to produce welded fabrications, to build up and hard face for the entire plant; to supplement the operating division forces with welders and burners on downturns, general repairs, rebuilding and construction projects; and, finally, to carry a stand-by group on afternoon and night shifts for assistance to "around the clock" operators. The Weld Shop facilities operate on a three-turn basis, and when field men are not needed for outside duty they work in the shop.

Frank J. Gaydos is General Foreman of the United States Steel Co., Gary Steel Works, Welding Department, Gary, Ind.

Presented at the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Jan. 15, 1953.

Figure 1

This brings us to the point of field operations. We have 48 welding units assigned to field work. Of these, 35 are electric motor generator sets and 13 are self-propelled mobile units. The mobile units are equipped with a 300-amp welding generator having an oversize exciter to provide 110 v current for grinders or extension lights, a set of oxygen and acetylene cylinders as well as welding leads and a rod bin equipped with various sizes of E6010, E6012 and low-hydrogen electrodes (Fig. 1).

These mobile units have at times taken care of as many as four jobs daily in remote places of the plant. Their usefulness comes into play by the elimination of transport and temporary electrical connections; however, for heavy welding, build-up, and long term jobs in repair and rebuilding, the electric motor generator sets are more practical and economical.

The goal of preventive maintenance in production facilities applies with equal force to maintenance equipment. A motor mechanic, on the Weld Shop roll, checks all mobile welders on the midnight turn, when the demand is the lightest. He cleans, greases, checks oil and water and has all the units gassed up, ready to go in the morning. It is his duty to overhaul motors and transmissions, repair tires, etc. Due to these precautions and preparations we rarely lose time due to failure of welding units on the shifts carrying the heaviest demand, namely, the day and afternoon shifts.

There are awkward places encountered, such as in open-hearth basements, blast furnace high lines, skip houses, ore unloaders and bridges, cranes, etc., where our Electric Line Department uses the services of

welders for fastening such things as conduit, brackets, junction and switch boxes, etc. On these jobs the use of a motor generator set is time-consuming due to constant moving of the equipment in close quarters, to uneven floor conditions and to stretching of welding leads over excessive distances. The larger mobile welders are too wide to maneuver in some places, or due to railroad tracks and walls they cannot be brought close enough to the job. Recently, we have added to our field equipment several 150-amp, d-c air-cooled gasoline welding units each weighing about 400 lb and used to handle these difficult field jobs. One man can transport his machine through openings as small as 32 in. wide and it is easily hoisted to the top of cranes, where the walkways are about 36 in. wide. These units can also be lifted with little effort by three men onto the bed of a truck for transport to other locations, thereby eliminating the waiting time for crane lifts. These units allow the effective use of only a $^5/_{32}$-in. diam electrode. The usage demanded, however, is no more than that, and they save many man-hours of moving, plus the time savings of gangs they service.

These "baby" units, do not, as a rule, return to headquarters at the end of a turn, but have divisional "doghouses" where they are stored at the end of a day's work. About once a month they are brought into the Weld Shop garage for a thorough check. On the job they are serviced by the operator.

A motor inspector is assigned to checking and servicing the motor generator sets. He covers as many as possible during the day, checking their operation, greasing bearings where required, and if there is possible trouble in the offing, he orders the machine into the Electric Repair Shop for further check, repair or clean-up. It should be said in passing that all welding machines under the jurisdiction of the Weld Shop are sent to the Electric Shop on the average of once every 18 months for clean-up and overhaul.

The Central Weld Shop is the headquarters for welded fabrication, hard facing and all automatic welding applications.

This shop is equipped with three welding lathes of 44, 36 and 18 in. swing capacities, and all are provided with automatic heads for using coated automatic or submerged-arc welding wire. The heads, wire reels and submerged-arc melt recovery equipment are mounted on a frame connected to the lathe lead screw for cross travel, enabling all deposits to be made helically.

Gary Steel Works began the use of build-up on a lathe nearly 12 yr ago, and we highly recommend its application to all maintenance shop operators for its potential savings in man-hours, not only in welding, but also in the machining operations.

The savings in welding are realized in greater deposition rates in reduction of overwelding and in the ability to deposit a concentric weld common to centers which will be used in further operation.

Machining savings are achieved by the reduction of excessive build-up and by the virtual elimination of high spots which commonly occur during manual build-up. The high spots from overwelding are time-consuming in a machine shop because of the rotation time necessary to true-up the lop-sided contours. High spots and interrupted cuts cause tool wear and breakage and also unsatisfactory impact reactions.

Figure 2 illustrates the quality of build-up produced on one of these lathes. This is a journal of a slabbing mill table roller, which runs in bronze bearings. Note the even spacings of the beads, and the lack of high spots. From this illustration one can readily visualize the savings of lathe time to finish this type of a build-up. This is a routine job, performed by submerged arc welding. A 55/65 carbon wire was used, and the lead on the lathe screw is $^3/_8$ in. per revolution.

Figure 3 shows the build-up of Vee-groove table roller using the submerged-arc welding process. A ball of steel wool is used, as illustrated, to hold the melt in place while the build-up is in progress on the sloping sides of the grooves. The same technique was used to weld up the sloping sides of the journal of the slabbing mill roller shown in Fig. 2.

Effective savings of machining time can be achieved by the use of the technique shown in Fig. 4. The problem was to provide a 5-in. diam journal section on a $3^1/_2$-in. diam shaft, 12 ft long. The upset portion of the shaft was built-up to $5^1/_2$ in. diam, allowing for machine finish, on a $3^1/_2$ in. diam shaft. One can readily ap-

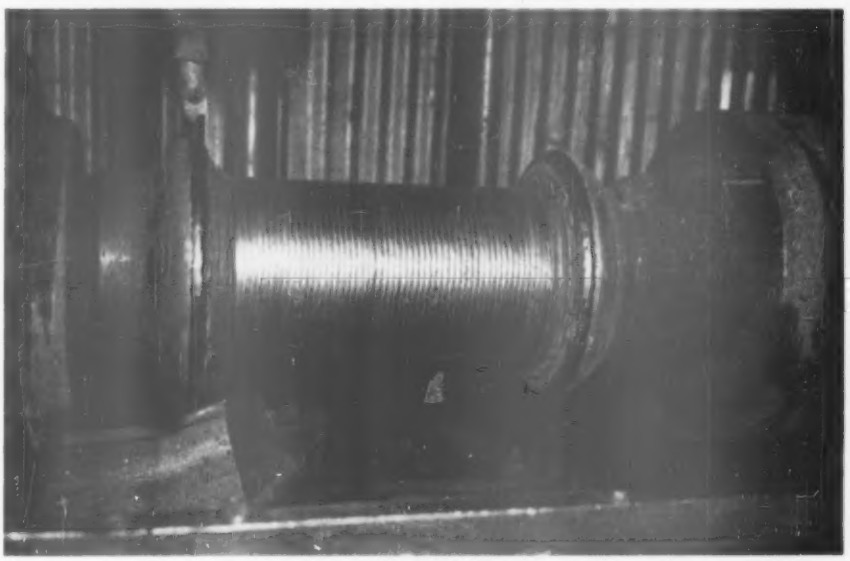


Figure 2

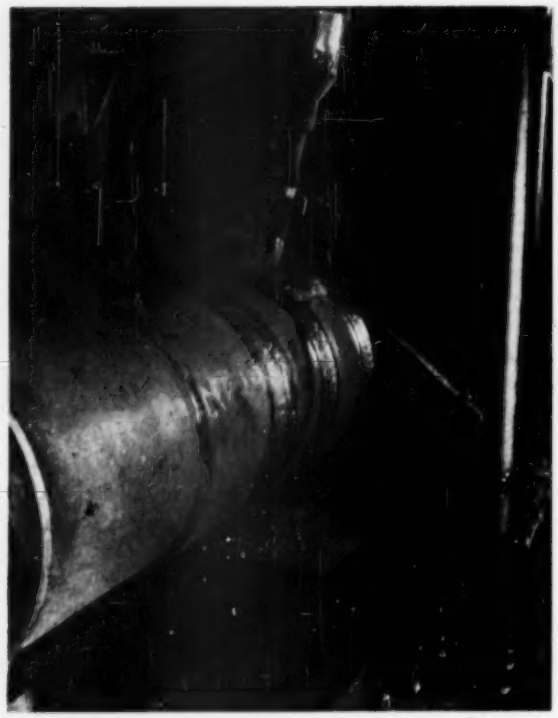


Figure 3

preciate the additional expense had this shaft been either forged or cut from a larger diameter round.

The question may arise as to warpage of shafts fabricated in this way. As a matter of fact the heat is distributed evenly about the perimeter of the shaft due to rotation during the welding operation and thus warpage is minimized. Should a slight warp develop it can be easily straightened using spot heating with an oxy-acetylene torch. It is desirable to have the shaft rotate in the lathe after completion of welding to permit an even cooling of the heated section in the free air.

Figure 5 is an example of a large job in one of the lathes. This is a blooming mill roller shaft, 25 ft long, weighing 27 tons, and is used by pairing it with another identical roller. These rollers are used to support and rotate round blooms as they are cut to length for blanks that will be used in forging steel railroad wheels. The friction saw used creates a tremendous reactionary force at the end of the shaft near the lathe faceplate. The first four to six spaces are periodically built up, using a 55/65 carbon wire and faced with a manufactured hard-facing wire of about 3% alloys, depositing a 30 to 32 Rockwell C hardness layer. This deposit is easily machined using high-speed tool steels and does not have a tendency to mark the cut blooms which are rigidly inspected for the slightest defect before being used as blanks in the forging of our present-day high-speed railroad equipment. Note in this illustration the use of rollers in the place of a tailstock and another at the faceplate. These rollers are blocked up from the floor to take the load off the body and the center of the lathe. It is also necessary to accommodate the length of the piece. This illustration also shows the rigging used to carry the welding head, controls, melt recovery equipment and wire reels. The rigging is fastened to the travel carriage of the lathe and driven by the lathe lead screw.

There are many cases where machining can be eliminated, if a practical approach to the problem is adopted and a consistently good automatic welding job is performed. Such an example is shown in Fig. 6, which shows a cast table roller for conveying hot rolled prod-

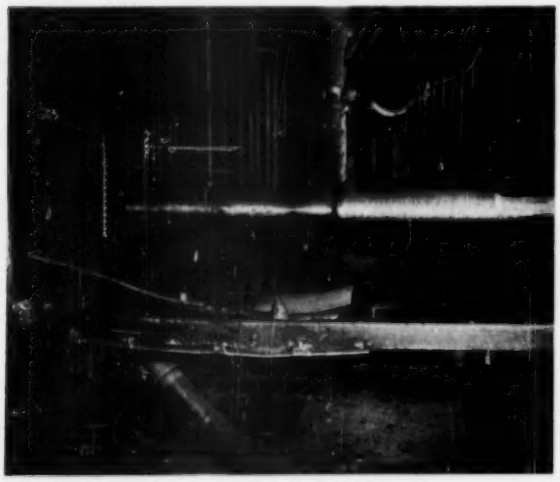


Figure 4

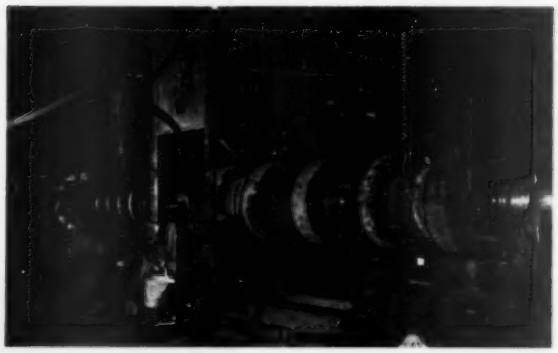


Figure 5

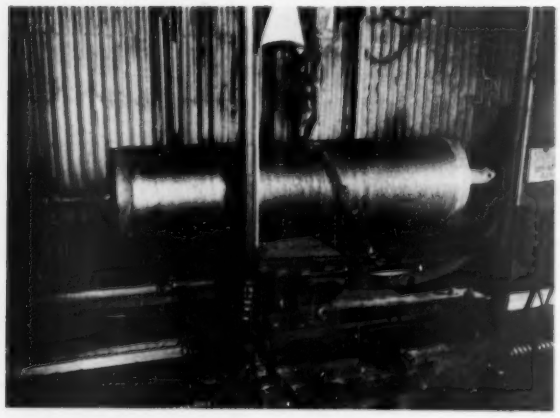


Figure 6

ucts on one of our merchant bar mills. This roller was originally cast and machined. After considerable wear it was sent to the Weld Shop for salvage. This roller was built up to within 1/4 in. of its original diameter and given a final pass of an 8% alloy hard-facing wire whose principal alloys are chromium, tungsten and molybdenum.

When this application first developed, rollers were built up with high carbon wire and machined. The next step was to hard face the body surface and grind to a smooth finish. A study of this application was made with the cooperation of the rolling mill operator and it was determined that an "as-deposited" surface would be better because it provided more traction to the product if the welding operator would produce a consistently good and even surface.

The depth of the grooves formed by the overlapping beads does not exceed 1/64 in. Sometimes a knob is formed during deposit, due to the interruption of the melt feed and of the time proper weld progression is reestablished. In that event, the knob is ground off using a pneumatic hand grinder after the welding is completed. The 2-in. wide metal strapping tacked to the ends of the roller body for the purpose of supporting the melt is removed before shipment to the operator.

By the use of hard facing, the life of such rollers has been increased from 4 months to as much as 2 yr and a costly machining and grinding operation is also eliminated. We are now fabricating many of these rollers from steel pipe and hard facing them before they are put in service.

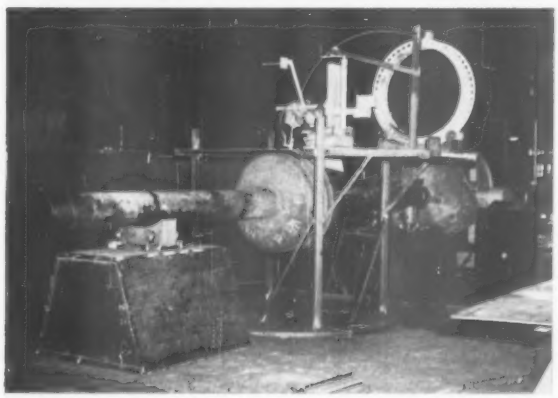


Figure 7

Cylindrical build-up need not be restricted to a lathe as illustrated in Fig. 7. Normally the gas engine piston shown is worked on in the 44 in. lathe, but at this time the lathe was being used for another large job, therefore, illustrated rigging was made up, using a welding positioner for rotation of the piece. Rollers are used at each end to support the workpiece. A temporary frame carries the heavy-duty cutting machine on which is mounted the welding head. In the operation shown, the piston ring grooves are being welded for remachining. This type of a setup can be used for surface build-up also, but inasmuch as a helical feed is not practical on this arrangement, a roller contact switch is attached to the turntable of the positioner and connected to the cutting machine carriage. At each revolution the switch is contacted with a cam welded to the positioner turntable and the carriage moves a total of 3/8 in., which has been predetermined with the speed setting of the cutting machine carriage.

The applications shown are routine jobs and illustrate the use of various methods to perform effective salvage work. These are but a few examples of assistance to an operator; where machining was eliminated, he was able to reduce costs and also received a better product. The maintenance welding man has unlimited opportunities to reduce costs by a thoughtful approach to every problem, not only in his own operations but also in the operations following his own, which include machining, grinding, forging, heat treating and erection. Careful consideration should be given to the possibility of simplifying or eliminating some of these operations.

The versatility and ingenuity of the maintenance welding personnel are called into play more concretely when replacements are required for equipment. Many of the machinery elements are castings manufactured as far back as a generation and a half ago, thus precluding the availability of patterns for replacement castings. It is then necessary to study the function of the replaced part, discarding the physical design which was used and thinking of replacement with available materials on a functional basis. One must remember that cast parts are made to suit patternmaking, coring, molding and pouring requirements and limitations. Therefore, if such parts are to be fabricated by welding processes, the same consideration should be given to this process and the casting need not be duplicated exactly. The required centers, clearances and spacings should be considered. Starting from these points we can build the replacement from rolled steel products, such as plate, structurals, bar, round or pipe stock; keeping in mind the elimination of machining operations. In casting procedures, spot facing is necessary but is not required when plate is used because a plane surface is realized. Pads for mounting motors, reducers, etc., do not need to be machined when they are to be shimmed up for perfect alignment. Very often it will be found that a combination of simple castings and rolled products will be a practical and economical solution.

An example of composite fabrication is the charging boxes used to fill open-hearth furnaces. There are thousands of these boxes in daily use and the Weld Shop has been assigned a standing order to fabricate this staple item using welded construction. Figures 8 to 11 illustrate the various stages of the manufacture of these boxes.

The charging box is composed of two castings, one an end casting and the other a peel head casting having a rectangular pocket. The peel head casting engages the peel head of a charging machine shaft which is used to lift the box from the buggy and to turn it in emptying

Figure 8

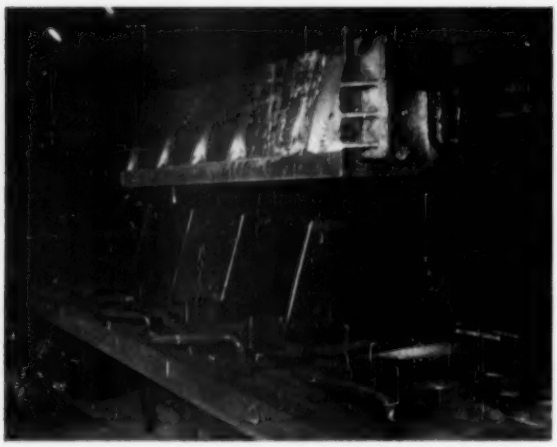


Figure 9

the contents into the furnace. In an inverted position, the box is also used to level the burden of the furnace. The body is composed of three plates and two bars which form the rim. The bars and body plates are cut on a shape-cutting machine from large plates. This cutting has been employed for the purpose of obtaining parallel edges and shapes of correct length and width. This is necessary to insure proper alignment of joints and to eliminate misfits along the seams to be welded by the submerged-arc process.

Figure 8 shows a box assembled and tack welded, and Fig. 9 shows the simple jig used for assembly. Only two overhead crane movements are required for completion of one box; one to deliver from the assembly table to the automatic welding setup and the other to deliver the box to the shipping area. The assembly is performed under an air-hoist equipped jib crane.

After the assembly is completed, the box is placed in the welding jig shown in Figs. 10 and 11. A 2000-lb capacity, hand-operated positioner is mounted on a steel plate bed using a reversible air motor for rotation of the faceplate. This faceplate is provided with a peel head to engage the socket of the charging box similar to that used on the charging machine. The tail end is a trunnion provided with a shelf and centered with the faceplate arrangement. This setup is, in turn, mounted on a buggy, equipped with a turntable. The

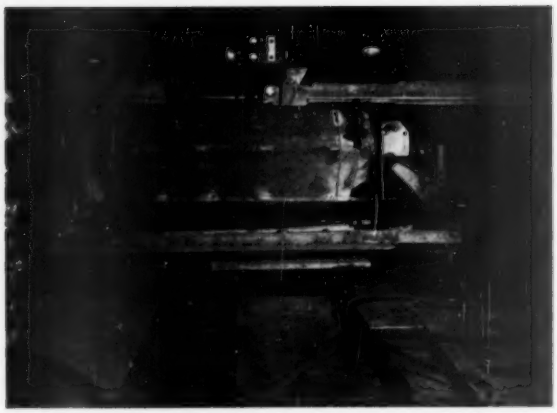


Figure 10

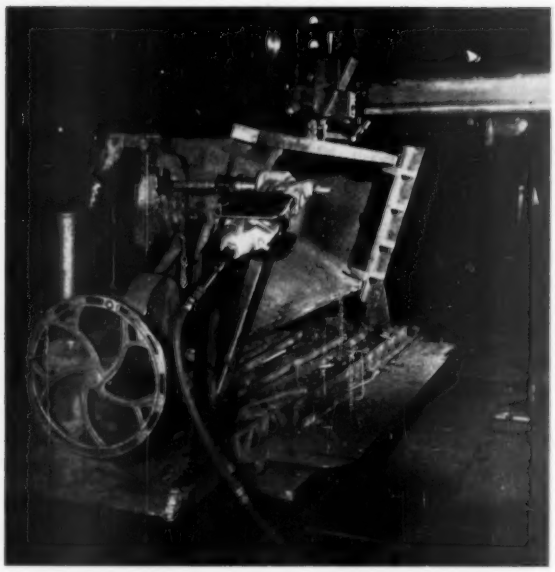


Figure 11

buggy runs on a Tee-bar section track being propelled with another reversible air motor.

With this arrangement, the box can be rotated to the best welding position for manual and submerged arc welding, and can be swung around to take care of both longitudinal and transverse welds, completing all operations without the use of an overhead or a jib crane lift.

Two operators now weld all the inside joints manually to form a root pass and to prevent break through when the outside seams are welded with the submerged arc. Figure 10 shows the position of the jig during the longitudinal seam welding and Fig. 11 during the transverse welding sequence. After completing the automatic welding the operators heat the bar forming the rim of the box, clinching it around the peel head casting. They burn off the amount in excess of that needed for leverage in bending and welding the clinched end to the casting. At this time any craters or burn-through portions are manually patched up making the box ready for shipment.

The entire operation of producing a composite type of welded charging box consumes only 12 man-hours of labor, and includes shapecutting, assembly, manual and automatic welding and patching.

As has been stated in the opening remarks of this paper, the examples illustrated are daily routine assignments. The approach to these problems is important. It should include consultation: (1) with the operator as to his requirements for producing a job to his satisfaction from the standpoint of service and cost; (2) it must consider machining or other operations that must follow welding, such as limitations of available equipment to finish a welded job; and (3) it requires the use of one's ingenuity to produce a staple item at a minimum expenditure of man-hours with the facilities that are at one's disposal. The maintenance man can be justly proud of his accomplishments and of his importance and position in any production picture.

PRACTICAL WELDER AND DESIGNER

How to Repair a Tubular Handle

by F. C. Geibig

TUBULAR parts frequently break across bolt holes where they are weakest. You can make a repair and strengthen such a weak point if you insert a reinforcing sleeve inside the tube. Then braze weld the break and redrill the bolt hole.

For the reinforcing sleeve, use pipe that fits snugly into the tube. The best job is done by fitting the sleeve so snugly that it has to be tapped into the tube ends. A piece 6 to 8 in. long, with at least 3 in. on either side of the break, makes a strong splint.

After straightening and rounding the broken tube ends, bevel the edges with a file. Leave three or four sections about $^1/_8$ in. wide undisturbed as guides for lining up the broken edges. Clean off dirt, scale rust, grease and paint for at least 1 in. back of the break. Also clean the sleeve for its entire length. When you bring the tube edges together with the reinforcing sleeve inside, leave a $^1/_{16}$- to $^1/_8$-in. gap between the two parts so melted bronze rod can penetrate to the sleeve.

Use your welding blowpipe to heat the area around the break. As the metal becomes hot, concentrate the heat on a small spot on the side of the tube until both edges of the break turn a dark red. Dip the bronze welding rod into a can of Brazo flux and touch it to this red spot on both sides of the break. Continue adding rod metal to build up a shelf for the weld puddle. After the puddle forms, build up the weld toward the top of the tube. This way you are welding uphill. If the weld puddle becomes too liquid, it will run away. When this happens, remove the flame until the puddle cools to mushy consistency.

When you reach the top of the tube, stop welding and turn the work a quarter turn. This puts the end of your weld back at the side. Now reheat the weld to melt the puddle again and once more build up to the top. As the brazing proceeds, the exposed part of the reinforcing sleeve should be tinned and rod metal built up so that the sleeve becomes a solid part of the welded repair.

Add rod metal until the weld is higher than the surface of the tube. If you need to add more metal after the first pass, reheat the weld metal until it is molten so that added welding rod metal will become part of the weld. Remember to use flux so that undissolved oxides do not destroy weld strength.

Before painting over the finished weld, clean off all traces of flux with steel wool or a wire brush. For any job where weld metal must be the same color as the tube to be welded, use welding rod of the same composition as the tube.

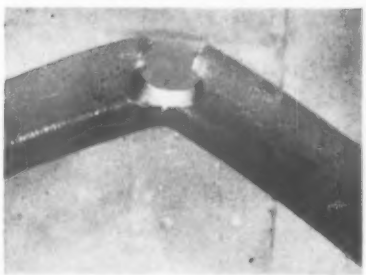


Fig. 1 The most frequent break in a tubular part is at the bolt holes. This spot can be repaired so that it is stronger than ever

Fig. 2 Heat the tube and it will fit easily over a tight-fitting sleeve. Notice that the sleeve has been cleaned until it shines

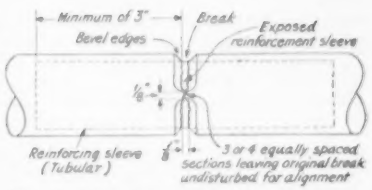

Minimum of 3"
Break
Bevel edges
Exposed reinforcement sleeve
Reinforcing sleeve (Tubular)
3 or 4 equally spaced sections leaving original break undisturbed for alignment

Fig. 3 The sleeve should extend into each part of the tube for a minimum of 3 in.

F. C. Geibig is connected with the Linde Air Products Co., Newark, N. J.

How Proper Design in Steel Saves Material

The data on "equivalent steel sections" outlined in this paper are taken from the Lincoln WELDesign System. It will help establish the proper amount of steel to use when converting existing cast designs to steel or when developing new products

Designing for Rigidity . . . All Loads*

Since steel resists deflection $2^1/_2$ times better than run-of-foundry gray cast iron, a steel component of unit cross section, for example, will support a load of 2500 lb whereas a similar component in cast iron supports only 1000 lb (Fig. 1). When redesigning a component for steel, therefore, only 40% of the cross-sectional area is generally required to duplicate resistance to deflection.

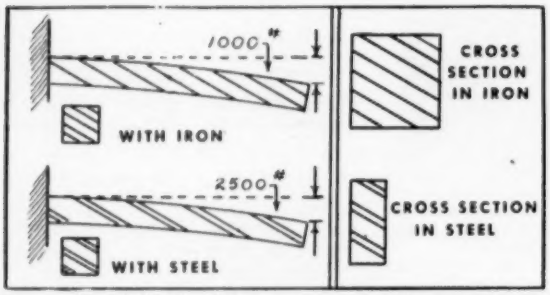


Fig. 1 (Left) With the same cross sections and same allowable deflection, steel supports $2^1/_2$ times the load possible with iron. (Right) For equal resistance to deflection, steel needs only 40% of the sectional area required in iron

Designing for Strength . . . All Loadings Except Pure Compression†

Considering safety factors of 5 for steel and 8 for iron, the relative design strength of steel to gray iron can be based on the ratio of 4 to 1. Accepted average tensile strength of steel by consultants is 60,000 psi. Run-of-foundry gray iron runs 24,000 psi. In designing for strength, therefore, the cross section in steel need only be 25% of the original iron sections, providing the same relative depths can be retained (Fig. 2).

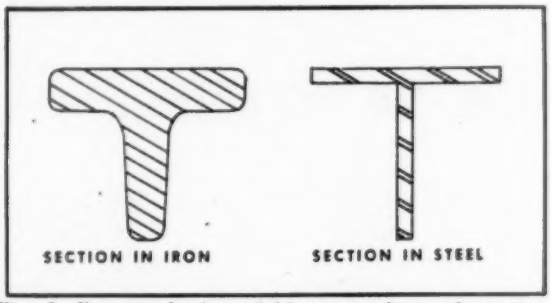


Fig. 2 For equal, dependable strength, steel sections require only 25% area of original iron design

While safety factors accepted for standard design practice are 5 for steel and 8 for iron, the type of product and service expected often calls for further modification of values as indicated in the chart (Fig. 3). The factors of safety shown are based on the use of the ultimate tensile strength in design.

	CAST IRON	STEEL
Bases	8	5
Containers	6	4
Levers and Links	20	15
Cranks	9	6
Brackets	6	4
Gears and Flywheels	11	7
Flywheel Rims	20	8

Fig. 3 Safety factors commonly used in current machine design practice

Designing for Pure Compressive Strength

The low average ultimate compressive strength of run-of-foundry gray iron is 95,000 psi. Relative factors of safety in compression are 5 for steel and 8 to 9 for iron. In pure compression, iron would be as suitable an engineering material providing it could

* Designs in which satisfactory performance requires a minimum amount of movement or deflection.
† Designs in which criteria for failure is permanent distortion.

be purchased as a casting on a pound-per-pound basis equal to steel. However, considering rigidity under compressive loads, steel resists deflection $2^1/_2$ times better than iron and, therefore, requires 60% less metal to do the same job.

Basic Design Requirements

This discussion thus far has pertained to mild steel and run-of-the-foundry gray iron, since these materials are the most commonly used in manufacturing today. In use also are materials such as the "semisteels" or high tensile irons having strengths of 30,000 to 50,000 psi. Malleable irons and the nodular irons having tensile strengths of 50,000 psi are also available at considerably higher cost. The following sketches compare the relative cross-sectional areas for machine members in iron and steel according to the type of loading. *The figures indicated on the drawings show comparative areas in iron with an "equivalent steel section."*

Savings in Materials by Use of Equivalent Steel Sections

1. FOR TENSION

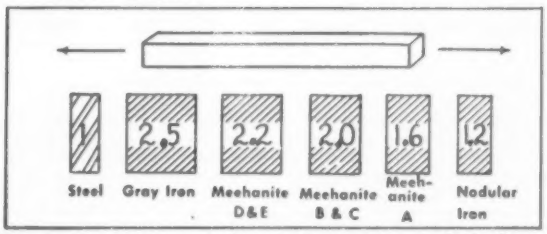


Fig. 4 For rigidity, steel equivalent section is 40% of gray iron section. Material cost is 15%

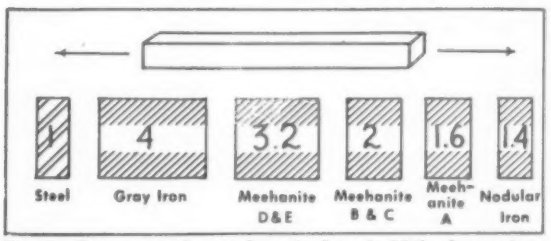


Fig. 5 For strength, steel equivalent is 25% of gray iron. Material cost is 10%

2. FOR COMPRESSION (long column)

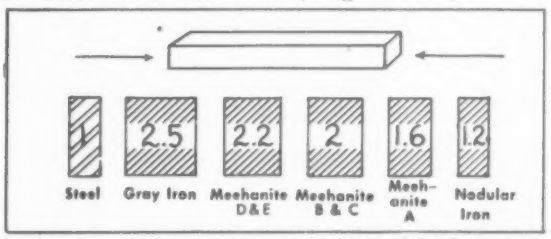


Fig. 6 For rigidity, steel equivalent is 40% of gray iron section. Material cost is 15%

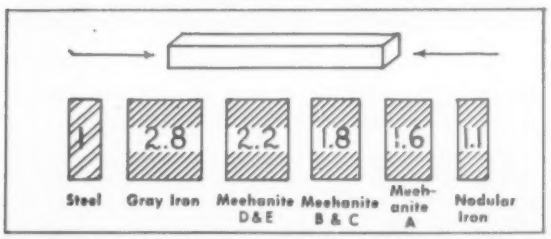


Fig. 7 For strength, steel equivalent is 35% of gray iron section. Material cost is 13%

3. FOR COMPRESSION (short column)

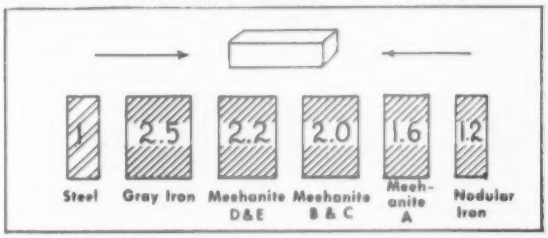


Fig. 8 For rigidity, steel equivalent section is 40% of gray iron section. Material cost is 15%

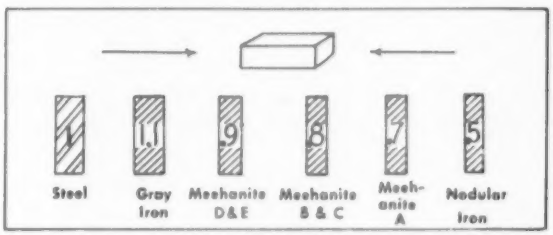


Fig. 9 For strength, steel equivalent section is 90% of gray iron section. Material cost is 34%

4. FOR BENDING

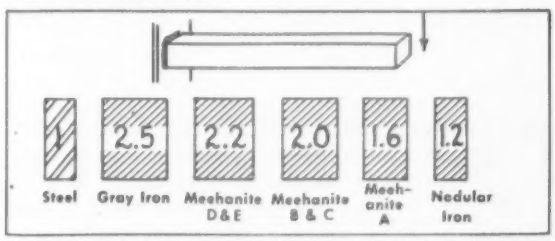


Fig. 10 For rigidity, equivalent section in steel is 40% of gray iron section. Material cost is 15%

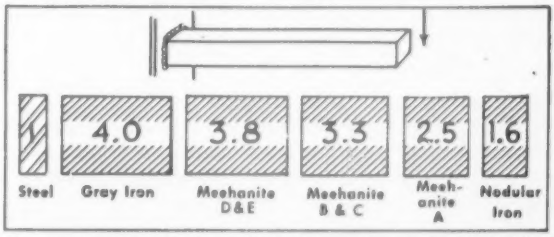


Fig. 11 For strength, equivalent section in steel is 25% of gray iron section. Material cost is 10%

5. FOR TORSION

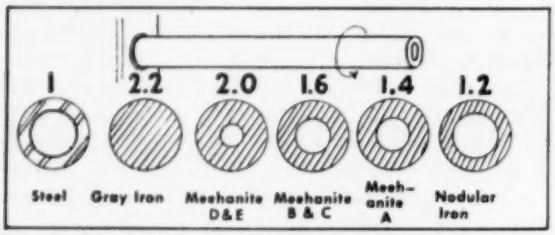


Fig. 12 For rigidity, equivalent section in steel is 45% of gray iron. Material cost is 15%

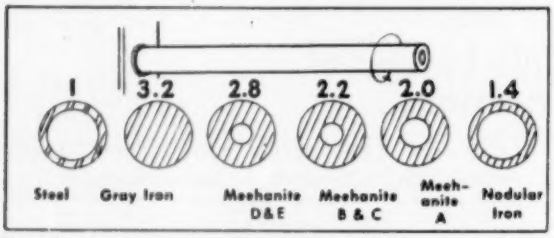


Fig. 13 For strength, equivalent section in steel is 31% of gray iron. Material cost is 12%

Guide to Allowable Costs

Most production savings are achieved on product conversions where simply the correct amount of steel or "equivalent sections in steel," as shown in the previous comparisons, are incorporated into the design. The table (Fig. 14) provides a rule-of-thumb comparison on which to estimate savings with welded steel. The figures shown are based on shop costs of $2.00 per hour and 200% overhead. Welded steel designs that do not already average 50% savings can be analyzed similarly to detect excessive fabricating expense.

HOW TO SAVE 50% ON COSTS

CASTING		WELDMENT			
AVERAGE DESIGN		EQUIVALENT MATERIAL DESIGN		50% EXCESS MATERIAL DESIGN*	
Weight (lbs.)	Cost ($)	50% Cost Reduction	25% Cost Reduction	50% Cost Reduction	25% Cost Reduction
		Allowed Labor Time (hours)		Allowed Labor Time (hours)	
10	1.60	.11	.17	.09	.16
20	3.20	.22	.35	.18	.32
40	6.40	.43	.70	.36	.63
60	9.60	.65	1.05	.55	.98
80	12.80	.70	1.40	.73	1.27
100	16.00	1.17	1.75	.92	1.59
200	32.00	2.27	3.50	1.83	3.17
300	48.00	3.25	5.25	2.75	4.75
500	80.00	5.42	8.75	4.60	7.90
700	112.00	7.60	12.25	6.40	11.10
1000	160.00	10.85	17.50	9.15	15.80
1500	480.00	16.25	25.40	13.75	23.80
2000	320.00	21.70	35.00	18.35	31.70
2500	400.00	27.10	43.70	22.90	39.50
3000	240.00	32.50	52.50	27.50	47.50

*Products with design sections having 50% more steel than equivalent needed to carry loads.

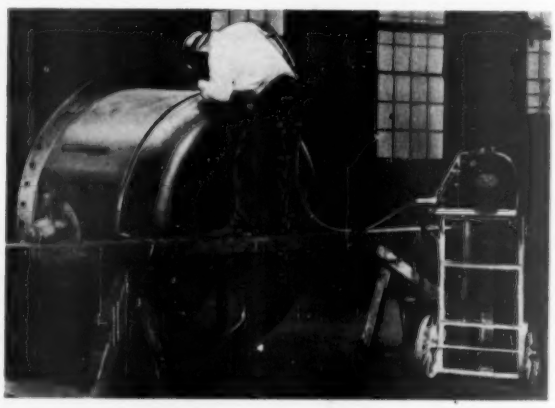
EXAMPLE: A gray iron casting weighing 60 pounds normally costs about $9.60. When redesigned for steel, initial savings in material cost provide an allowable labor time of .65 hours for fabrication to realize overall savings of 50%. 1.05 hours of labor can be expended if costs reductions of 25% are satisfactory. Where 50% more steel is used than necessary, .55 hours labor can be spent to achieve 50% overall savings or .98 hours expended for a 25% cost reduction.

Figure 14

Production Welding Large Copper Vessels

CONCENTRATED heat input and speed of deposition are essential for sound welds in thick sections of copper. The Aircomatic process, employing the new No. 20 manual gun, meets these requirements with greater ease and economy than ever before possible. The ability of this unit to handle currents up to 500 amp at high-duty cycles makes it indispensable for production work such as that being done at Ansona Copper and Iron Works, Inc., of Cincinnati, Ohio.

This company is fabricating acetate mixing vessels, 6 ft in diameter and 3 ft deep, from 1 to $1^1/_8$-in. thick copper. For the first time it is possible to weld heavy copper without preheat. Deoxidized copper welding wire, $^1/_{16}$ in. in diameter is fed into the weld from the Gun at the rate of 396 ipm. Two 400-amp generators,



in parallel, provide the required 470-amp, d-c reversed polarity. The shielding gas is a combined helium-argon mixture. Gas flows are 50 cfh helium and 20 cfh argon.

The joint for the girth weld on the vessels is a single vee with a 90 deg included angle and a $^3/_{32}$-in. root opening. The welds are made in four passes from the face side, back chipped and a single root pass made.

the AMERICAN WELDING SOCIETY

activities • related events

Please Help!

If you can spare your copies of any of the following 1952 issues of THE WELDING JOURNAL we would like you to return them to the SOCIETY—attention of F. J. Mooney, Assistant Secretary:

March, April, September and October

New Sustaining Member

Deere & Co., Moline, Ill., is a pioneer in the farm equipment industry tracing its origin to the development of the world's first successful steel plow, in 1837, by its founder, John Deere.

Today, the John Deere organization embraces 16 factories manufacturing a full line of agricultural tractors and farm implements marketed through a network of branch houses and independent dealerships throughout the United States and Canada, and through an Export department serving farmers throughout the world.

W. L. Palmer—Sustaining (A)-Member Representative.

Address F. L. Plummer*

The honor of being President of the AMERICAN WELDING SOCIETY is a very great one. Thinking back of the names of the 28 men, all men of great stature, who have preceded me—Jennings, Pierce, Fraser, Sieger, Hill, Boardman, Crowe, and all the others, back to our founder and first great President, Comfort Adams—I accept with a deep sense of humility, a strong feeling of grateful appreciation and a full awareness of the responsibilities.

For the past 25 years I have, in a small way, participated in the local, regional and national activities of the SOCIETY. I feel I know something about the capabilities of the men and women who make up our membership. If this were not so, I would hesitate to accept these responsibilities.

The administrative wheels of our SOCIETY run so quietly that our members forget that back of the scenes their Officers and Board of Directors are establishing policies which an efficient headquarters staff—Magrath and Mooney, Spraragen and Rossi, Greenberg, Mrs. Schramm, Miss O'Leary and many others—capably carries out.

Our Society is thriving—sound and healthy financially; a growing membership; more than 100 active technical committees; significant local and regional activities; an outstanding publication program; many standing and special committees. It has been most gratifying to me to note the large number of competent, busy men—leaders in their special fields—who have signified their willingness to give of their time and talents in planning, organizing and directing the widely diversified activities through which we serve our members, industry and the nation.

In our nation today, there is a real threat that operation of a competitive economy may be choked off by the imposition of paternalistic state controls and restrictions on private enterprise. With your help and that of all the more than 8000 members of our SOCIETY, we can demonstrate anew that technical men—given freedom of action—are our truly modern pioneers, discovering new continents, their eyes on the far horizon, providing a comradeship in finding the best ways to do welding work of all kinds and in spreading that knowledge. I pledge my very best efforts in attempting to promote the best interests of our SOCIETY.

President's Address

Chas. H. Jennings

The AMERICAN WELDING SOCIETY is a group of individuals united with the common aims and objective of promoting the art and science of welding. Its success, growth and importance is directly proportioned to the effort each of us expends in accomplishing our common goal. I am happy to be able to report to you that during the past year our accomplishments have been noteworthy and reflect the interest and hard work that has been done by the members, the staff, the section officers, the board of directors and the officers.

Last year President Pierce stated that the SOCIETY was experiencing a reversal of trend because for the first time in six years there was a positive gain in membership, and for the first time in four years the SOCIETY was "in the black." This trend has continued at a definitely increasing rate. Delinquent members fell to the lowest percentage in the SOCIETY's history and the active membership has been increased by 563 members.

Finances are not only "in the black," they are in the real "black-black," thanks to expert budgeting and guidance by the finance committee and astute management by the headquarters staff.

During the past three years which I have served as an officer, I have had the opportunity to visit 20 sections. (I only regret I was not able to visit every one.) During these visits I was greatly impressed with the enthusiasm and interest shown by the members and section officers.

It is easy for me to see why section activities have played an increasingly important part in making this an outstanding year. Meetings have been more frequent and the average attendance larger. Special efforts have been made in many cases to adjust the programs to meet the membership desires. Joint meetings with other societies, inspection trips and educational courses were only a few of the ways sections are extending their services to the members and American industry.

Three new sections have been formed and several others are ready for charters. Progress has been made to reactivate several old ones that fell by the wayside after the war. This section interest is both encouraging and healthful because it illustrates the desire and need of industry for more welding information. Your SOCIETY has the experience, background and knowledge to furnish this help and it is encouraging to report the way it has responded.

Committee work reflects the hard work of many hundreds of members. Several new and important committees have been formed, several old ones have been reorganized and others are in the process of formation. Beneficial results are already being obtained from this program but it will take some time until the full benefits are realized.

Technical activities work has been stepped up by the aid of an assistant to the Technical Secretary. The importance of this work to the SOCIETY and industry in

* Acceptance address of newly elected President F. L. Plummer at Business Meeting, A.W.S., Philadelphia, Oct. 23, 1952.

general cannot be overestimated, consequently it is believed that continued effort must be expended along these lines.

For many years the Society has considered the desirability of holding more than one national meeting a year and promoting a welding exhibit. The advantages of such a program are many, but of primary importance is the opportunity it affords the many sections of the country other than those around a few major cities, the chance to gain technical knowledge and witness first-hand welding equipment, welding processes and allied products in operation.

Steps have been taken to expand the Society's national technical program and plans are well under way to hold a spring meeting and welding exhibit in Houston, Tex., during June 1953. This program is another step forward for the Society and represents another new era of service to the members and the welding industry.

The continued progress of the American Welding Society has been the result of the combined effort of the members and the staff. It is impossible for me to personally recognize the excellent work and assistance rendered by the many members and committees. I do, however, want to express my appreciation to the Board of Directors for their helpful guidance and support in directing the affairs of the Society. The actions taken have been progressive and farsighted. Progress is sometimes slower than desired although with the proper foundation ultimate progress is assured.

I also want to acknowledge the untiring services of Messrs. Magrath, Mooney, Spraragen and Greenberg for the efficient way they have carried out the duties of their offices. The many other members of the headquarters staff must also be commended for their efforts in the many phases of the headquarters operations.

It has been a pleasure and a wonderful experience for me to serve as President for the past year. The successes and advancements that have been made during my term of office are the result of the many individuals who have given untiringly of their time and energy. I can take little personal credit for our achievements and this is as it should be. As mentioned before, our Society is a group of many individuals and it is these individuals that produce our success.

The American Welding Society is growing in stature, importance and prestige. We have had many profitable years in the past but our future is brighter than ever. Your continued support and efforts, and the guidance of your incoming officers and Board of Directors assures the Society's continued success.

As I retire from the office of President, I feel that I owe the Society a great debt. The knowledge I have gained through my associations with other members, the friends I have made in the many sections and the experiences I have gained, appear to far outweigh my own personal efforts. The importance and need of a strong American Welding Society is more obvious than ever. I only hope that by continued service and efforts, I will be able to partly repay my debt to the Society.

J. B. Tinnon Elected Honorary Member

At a meeting of the Board of Directors held Mar. 31, 1952, James Whitfield Owens and John Baird Tinnon were elected to Honorary Membership in the American Welding Society. The awards were made at the Annual Meeting held in Philadelphia during the week of Oct. 19, 1952.

The citation for Mr. Owens stresses his pioneering efforts in the application of welding particularly in the marine construction field and his service to the Society in many capacities including that of director and Vice-President.

The citation for Mr. Tinnon highlights his pioneering efforts in the application of welding in the transportation industry, his work on many committees of the Society including the Board of Directors.

John B. Tinnon

John B. Tinnon was born in Fort Worth Tex., on June 21, 1885, son of Dr. Robert M. Tinnon, a Presbyterian clergyman, and Sarah Preston Tinnon. He attended the public schools at Lincoln, Ill., and Denver, Colo., completing his education at Vanderbilt University where he was in the class of 1908. He was first employed in 1908 by the Chicago City Railway in Chicago, Ill. (later the Chicago Surface Lines) where he held various positions: rod man, instrument man, inspector, welding supervisor and assistant engineer of maintenance of way. In 1912 he went to Joliet, Ill., and became engineer of maintenance of way; later chief engineer of the Chicago & Joliet Electric Railway Co. He was with the Ordnance Department of the U. S. Army for a period of time in 1918, returning to Joliet in 1919. In 1916 he married Edwine M. Munroe of Joliet, Ill. In 1924 Mr. Tinnon accepted the position of superintendent of the rail welding department of Metal & Thermit Corp. in New York and has been with that company ever since, having been successively manager of rail welding department, sales manager of welding division, general sales manager, and in 1942 was elected Vice-President, and in 1944 a Director.

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John B. Tinnon

Mr. Tinnon for many years has been active in the transit industry and for a number of years was a member of the various committees on way and structures and served as a member of the executive committee of the American Electric Railway Assn. (now American Transit Assn); he also served for a considerable period of time as chairman of the manufacturers advisory committee of this association. Since 1924 Mr. Tinnon has been active in the affairs of the AMERICAN WELDING SOCIETY, having served as a member of various committees, as chairman of the manufacturers committee and on the board of directors.

Mr. Tinnon took an important part in the introduction on a large scale of welded rails in street railways and in paved track on railroads to the extent that the Thermit rail weld became a standard in the electric railway industry. Mr. Tinnon with J. H. Deppeler, formerly chief engineer of Metal & Thermit Corp., pioneered the introduction of continuous welded rail on main line railroads and it was due to the efforts of the Metal & Thermit organization, under Mr. Tinnon's direction, that continuous welded rail first became acceptable to railroads for their main line track.

Mr. Tinnon is an elder and trustee of the North Avenue Presbyterian Church of New Rochelle, N. Y., and is active in Presbyterian church affairs.

During World War II, Mr. Tinnon served in many official and unofficial positions involving welding with the various governmental agencies and armed services.

Mr. Tinnon has three sons—John M. Tinnon, Manager of Sales, Air Reduction Chemical Co.; William M. Tinnon, connected with Drawalloy Co., York, Pa.; and Edwin M. Tinnon, in the Chemical Division of Metal & Thermit Corp., New York, N.Y.

Biography—James W. Owens

James (Jim) W. Owens was born Feb. 14, 1886, on the island of St. Eustatius, Dutch West Indies, where his missionary father was stationed. This island fired the first foreign salute to our flag, and had its capital ransacked by British Admiral for this act of friendship.

Jim studied Electrical Engineering at Brooklyn Polytechnic, and his early years were devoted to power station design and construction, and railroad electrification.

In World War I, he was appointed Welding Aide for the Navy to head an extensive welding research program at its Norfolk Yard. This program included comprehensive research on residual stresses, construction of battle towing targets; a pontoon for a 150-ton derrick; training program for naval constructors and welding operators; preparation of the first welding specifications for naval construction; origination of qualification testing of operators; and writing of a 650-page textbook—*Fundamentals of Welding* (Penton Publishing Co.). A foreword by Rear Admiral D. W. Taylor, then Chief Naval Constructor, stated "it is largely through Mr. Owens research and investigations that we made such liberal use of welding on naval vessels."

In 1926 Jim went to the Newport News Shipyard as its Director of Welding and

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188	338	700	1250	1800
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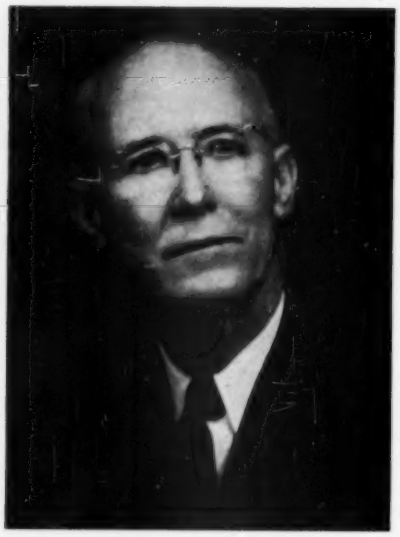

James W. Owens

applied welding extensively to the construction of merchant and naval ships. He wrote the first Welding Rules for American Bureau of Shipping; developed first weld gages; supervised design and construction of first welded fish belly crane; consulted on Mokolome pipe line; and was unanimously awarded by ASME the first prize of $10,000 in Lincoln Electric Co's first international competition (*Arc Welding*, McGraw-Hill Book Co.).

In 1930 he entered private practice and established the National Weld Testing Bureau of the Pittsburgh Testing Laboratory. In 1935 he was appointed Director of Welding of Fairbanks, Morse & Co., where for 16 years he developed welded construction of the Diesels that power more than 50% of our submarines, and a goodly proportion of our nation's locomotives. In 1949 he resigned and again entered private practice.

Jim was one of the organizers of the Society in 1919; its vice-president from 1921 to 1922; a Director on several occasions; first chairman of its Nomenclature and Symbols, also of its Marine Committees. The Society awarded him its Miller Memorial Medal in 1929. He is a former Fellow of the AIEE and Member of the Society of Naval Architects and Marine Engineers, also of the International Acetylene Assn., currently a member of ASME. He holds five patents, one for a trepanning tool; two for Diesel frames, and two for locomotive construction. He is a licensed Professional Engineer in New York and Wisconsin.

Acceptance Letter

October 30, 1952

The Board of Directors
The American Welding Society
33 W. 39th St.
New York 18, N. Y.

Subject: Honorary Membership

Gentlemen:

This is to let you know that I appreciate sincerely the Honorary Membership awarded me at the 33rd Annual Meeting of our Society.

I accept the award with a keen sense of the relative smallness of my accomplishments when contrasted with the enormity of other problems of national and international scope requiring solution.

I also accept the award with the feeling that it is the mechanism used by the Board to indicate its regret for certain incidents in the Society's activities in recent years which have caused me considerable pain. "The greater the love, the greater the pain." Undoubtedly, my own shortcomings were partly to blame.

In conclusion, it is consoling to contemplate that "perfection is never attained." Rather, it is a goal to be striven for in every phase of our individual lives, and in our National life. This is also true of a technical society.

With kindest personal regards for each and every member of the Board, and the Society's personnel.

Very sincerely,
James W. Owens

Final Meeting of Board of Directors, 1951–52

The final meeting of the Board of Directors of the American Welding Society for the 1951–52 year was held in the Red Room at the Bellevue-Stratford Hotel, Philadelphia, Pa., on Thursday, Oct. 23, 1952, at 3:00 P.M. The following were in attendance:

Members: C. H. Jennings, *Chairman*, F. L. Plummer, E. R. Seabloom, R. S. Donald, L. C. Bibber, T. J. Crawford, A. F. Davis, J. U. Durham, O. B. J. Fraser, R. S. Green, La Motte Grover, T. B. Jefferson, I. A. Oehler, H. W. Pierce, H. E. Rockefeller, M. S. Shane, G. N. Sieger, J. R. Stitt, C. B. Voldrich and A. E. Wisler.

Staff: J. C. Magrath, *Secretary*, and F. J. Mooney, *Assistant Secretary*.

Guests: J. E. Dato, District Vice-President, 1952–53; D. B. Howard, District Vice-President, 1952–53; J. H. Humberstone, Second Vice-President, 1952–53; I. Morrison, Director-at-Large, 1952–53; and R. L. Townsend, Chairman, National Membership Committee.

1. Iowa-Illinois Section

The Board was informed that a group centered in and about the cities of Davenport and Bettendorf, Iowa, and Rock Island, Moline and East Moline, Ill., had accomplished the enrollment of 50 AWS members and fulfilled the necessary steps for the establishment of a Section and formally requested authority to operate as the Iowa-Illinois Section of the Society with headquarters at Davenport, Iowa.

Action: Upon motion, duly seconded, the Board so approved.

2. Boundary Lines

T. B. Jefferson, Vice-President of District No. 5, advised that the Iowa-Illinois Section had requested approval of specific boundary lines, which would include the communities of Dubuque, Cedar Rapids, Iowa City and Clinton, Iowa, and Kawa-

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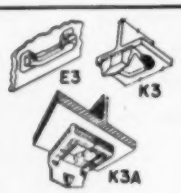

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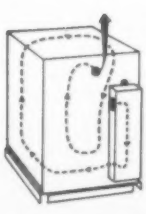

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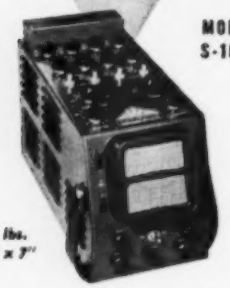

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12" x 6" x 7"

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nee and Sterling, Ill., and stated that these boundary line recommendations had been cleared with the Iowa Section in Des Moines, Iowa, and the Peoria-Central Illinois Section at Peoria, Ill., to their satisfaction.

Action: Upon motion, duly seconded, the Board approved of these boundary lines for the newly formed Iowa-Illinois Section.

3. Spring Meeting, 1953

The Chairman called upon the Secretary to inform the Board regarding developments to date since the Aug. 26, 1952, Board Meeting in connection with the proposed AWS Spring Technical Meeting and Welding and Allied Industry Exposition at the Shamrock Hotel and Hall of Exhibits in Houston, Tex., June 16–19, 1953. The Secretary reviewed the activities to date, the following through of the directions of the Board of Directors, at its previous meeting, including the issuance of advance publicity thereon containing advance reservation forms. He reported that better than 30 companies had made advance reservations as requested, which reservations constituted a total of over 6000 sq ft of space in the Exposition. The Secretary advised that a meeting of the Manufacturers Committee, 1951–52, had been held on Tuesday, October 21st, in Philadelphia, President Jennings presiding as Acting Chairman, whereat the subject had been discussed and recommendations made by the Committee and that the Exposition had also been discussed at the Section Officers' Meeting in Philadelphia on Tuesday, October 21st, whereat the various Section Officers had tendered their opinions and recommendations. Chairman Jennings called upon T. J. Crawford, Chairman of the Section Advisory Committee, who had presided at the open session of the Section Officers' Meeting to acquaint the Board with the expressions and interest of the various Section Officers attending. Mr. Crawford advised the Board that the Section Officers as a group were unanimously in favor of the proposed project and felt that it would be successful in that, in their opinion, not only the membership of nearby Sections would support the activity but that members of more distant Sections would want to attend. Chairman Jennings called upon H. E. Rockefeller who, as proxy for R. W. Boggs, had attended the meeting of the Manufacturers Committee on October 21st, to render a report on the discussions and recommendations of that group at their meeting. Mr. Rockefeller advised that the Manufacturers Committee was still of the same opinion as previously submitted to the Board of Directors, which was that they recommended that the Society should employ outside assistance for producing and conducting the Welding Exposition at Houston. Mr. Rockefeller made a motion that the Manufacturers Committee's recommendation that outside assistance be solicited and for the carrying out of further plans of exhibit, be referred to the Exposition Committee for study and appropriate action.

Action: Upon motion, duly seconded, it was voted that the Manufacturers Committee's recommendations, as aforedescribed, accompanied by the recommendations of the Board members as aforelisted, be referred to the 1953 Exposition Committee for study and appropriate action.

4. Recommendations on Section Officers and Sections

The agenda advised that at the annual meeting of the Mid-Southern District in Kansas City in summer 1952, the attending Officer delegates from 8 Sections went on record in recommendation that the titles of Section Chairman and Section Vice-Chairman be changed to Section President and Section Vice-President. A. E. Wisler, Vice-President of District No. 6, supported by R. L. Townsend, Chairman of the National Membership Committee, stressed that in the order of other Society custom, the titles of President and Vice-President were more commonly employed than Chairman and Vice-Chairman, the latter titles indicating the status of those who headed committees, rather than a complete activity. Further, that the recommended titles provided better community standing and recognition. They also observed that such received better recognition by banks. Mr. Wisler also observed that at that meeting recommendation was also made that the name "Section" be changed to "Chapter."

Action: Upon motion, duly seconded the entire matter was referred to the Section Advisory Committee for necessary study and report thereon to the Board of Directors at a subsequent meeting.

5. Membership Status

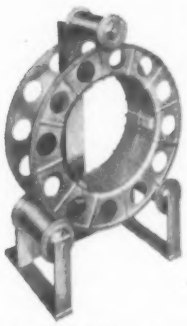
The Assistant Secretary stated that membership status report for the 1951–52 year was submitted to the Board of Directors on Oct. 2, 1952. He advised that the Society reported a net increase of 563 members as well as increase in Supporting Companies from 153 to 174. Further, he stated that for the month of September 1952, the Society reported gains of 222 members as against losses of 87, or a net increase of 135 members. The majority of these 87 losses were the result of member resignations based on August membership billing.

6. Membership Advertising.

The Chairman called upon R. L. Townsend, Chairman of the National Membership Committee, who proposed, in the order of agenda, that the Society utilize the advertising space of *The Welding Engineer* and *Industry and Welding* publications, in addition to The Welding Journal, for the coming year, six issues of each,

NOW

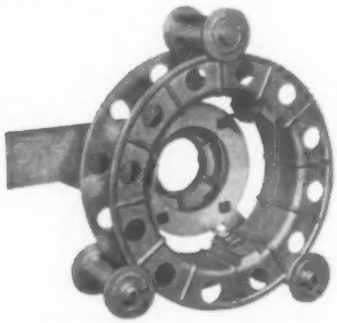
Double and Triple weight coils for automatic welding with the new REID-AVERY (free wheeling) REELS for Submerged Arc, Open Arc, and Gas Shielded Arc Welding



Raco Reel for 150-200# coils with floor mounting stand.

Decrease down-time by using larger coils. Machines now equipped for 25 lb. coils can use up to 75 lb. coils with these new reels.

These cast aluminum alloy reels ride on ball-bearing trunnions with deep flanges. The wire uncoils with remarkable ease and yet there is no chance of overrunning and throwing loops over the side. No brakes or drags of any kind are necessary. The wire itself prevents overrun. These "free wheeling" reels provide the absolute minimum load on the automatic welding machine.



Raco Reel for 25#-50#-75# coils. Mounted on popular submerged arc machine.

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We are equipped to supply all sizes of layer wound coils for these reels, for other reels, or on expendable wooden spools where required.

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for promoting membership. He outlined the necessity of bringing membership to the attention of the circulation provided by the three publications, a total of approximately 40,000, stating that such a program was essential to providing the necessary support by the National Society to the Section personnel involved in the activity of increasing the Society's membership. Cross discussion among Board members followed and it was the general consensus that the sum of approximately $5000 would be needed. A. F. Davis suggested that influence should be brought to bear upon the various welding equipment manufacturers, who produced and circulated house organs, to provide space in their house organs to the Welding Society for promoting membership without charge, and that his company will be very glad to do so in its house organ. He further recommended that the welding publications previously mentioned be again approached through our publicity channels to see if they could not provide a better rate to the Society for its institutional advertising.

Action: The preceding item was tabled with referendum that it be brought off the table at the meeting following of the 1952–53 Board of Directors.

7. Assistant District Vice-President

In the cross discussion on the preceding item, J. U. Durham, Vice-President of District No. 3, wanted to know what had happened to the recommendations that he and others had proposed that a new office be created which would be an Assistant District Vice-President, the reason for such being that the task assigned to the District Vice-President, generally over a large geographical area, was one that could not be properly fulfilled by one person, and that during a District Vice-President's term, his successor should have the opportunity of becoming educated in the requirements of the office so that when he was elected to the office of District Vice-President, he would have a full knowledge of the activities and be in a position to take over immediately. Further, by having an Assistant District Office, the District Vice-President's work would be cut in half in that he could assign certain areas or items of activity to the Assistant District Vice-President.

Action: Upon motion, duly seconded, the matter of creation of the office of Assistant District Vice-President was referred to the incoming Section Advisory Committee for study and report to the Board of Directors.

Closing Statements

The Chairman voiced his appreciation to all members of the Board for their cooperation during the past of his administration, and as President and Chairman of the Board, and expressed appreciation to the National Headquarters' Staff for the efforts it had expended during the year in serving the Society and the Board of Directors. The Chairman also announced that upon adjournment of this Board meeting, the first meeting of the 1952–53 Board of Directors would convene immediately and that President-Elect Plummer would preside as Chairman, and Mr. Plummer invited all outgoing Board members to remain and attend the first meeting of the 1952–53 Board of Directors.

Adjournment

The 1951–52 Board of Directors adjourned its meeting at 4:55 P.M.

First Meeting of Board of Directors, 1952–53

The first meeting of the Board of Directors of the American Welding Society for the 1952–53 year was held in the Red Room of the Bellevue-Stratford Hotel, Philadelphia, Pa., on Thursday, Oct. 23, 1952, at 5:00 P.M. The following were in attendance:

Members: F. L. Plummer, *Chairman*, E. R. Seabloom, J. H. Humberstone, R. S. Donald, J. E. Dato, J. U. Durham, O. B. J. Fraser, R. S. Green, La Motte Grover, D. B. Howard, T. B. Jefferson, C. H. Jennings, I. Morrison, I. A. Oehler,

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H. E. Rockefeller, M. S. Shane, J. R. Stitt and C. B. Voldrich.

Staff: J. G. Magrath, *Secretary*, and F. J. Mooney, *Assistant Secretary*.

Guest: R. L. Townsend, Chairman, National Membership Committee.

Appreciation

(*a*) Chairman F. L. Plummer voiced his sincere appreciation to outgoing President C. H. Jennings and to the Board of Directors for their activities and accomplishments during the 1951–52 administrative year.

(*b*) Chairman Plummer observed that a practice had been instituted the previous year, which he was glad to exercise this year, such being the presentation of an inscribed gavel and gavel block in case to the outgoing President, as Chairman of the Board of Directors, 1951–52. Chairman Plummer presented the gavel to outgoing President Jennings.

1. Staff Officer Appointments

Chairman Plummer called upon the Board of Directors for a motion appointing the Staff Officers of the SOCIETY for the 1952–53 administrative year, namely, the Secretary and the Assistant Secretary-Assistant Treasurer, the latter two combined in one office. Mr. Fraser moved that J. G. Magrath serve as the SOCIETY's Secretary and F. J. Mooney serve as the SOCIETY's Assistant Secretary-Assistant Treasurer for the 1952–53 administrative year.

Action: Upon motion, duly seconded, the Board of Directors so approved.

2. Documental Signature Authorization

Chairman Plummer called upon the Board of Directors for a motion authorizing the signing of checks and other documents in behalf of the SOCIETY. He observed that past practice has been to authorize the Treasurer and the President, and in the absence of either, the Assistant Treasurer.

Action: Upon motion, duly seconded, the Chairman's recommendation that the Treasurer and the President, and in the absence of either, the Assistant Treasurer, be authorized to sign checks and other documents in behalf of the SOCIETY, was approved.

3. Standing Committee Appointments

The Chairman called attention to a list of his recommended appointments to Standing Committee Chairmenship and Membership, as distributed to members of the Board of Directors attending the meeting. He respectfully suggested that the Board approve his recommendations in full, or he would be very glad to discuss them in detail, with the exception of the Manufacturers Committee. The Manufacturers Committee is now in the process of reorganization and he requested deferment of approval until such time as he is in a position to submit a complete organization listing for that Committee.

Action: Upon motion, duly seconded, the Chairman's recommendations for names of individuals to serve on all Standing and Special Committees, exclusive of the Manufacturers Committee, were approved.

4. Executive Committee Authority

The Secretary called attention to the action of the 1950–51 Board of Directors extending special authority to the Executive Committee, which action was again extended to the 1951–52 Board of Directors, in the order following:

"An Executive Committee consisting of not less than five (5) members of the Board of Directors shall be empowered to act for the Board of Directors in Administration of the affairs of the SOCIETY as may be necessary from time to time between meetings of the Board of Directors. Action taken by the Executive Committee shall be subject to approval by the Board of Directors at its next regular meeting."

The Chairman recommended comparable authority be extended to the 1952–53 Executive Committee.

Action: Upon motion, duly seconded, it was approved that the same special authority be extended to the Executive Committee for the 1952–53 year.

5. Membership Advertising

This item, discussed at the final meeting of the 1951–52 Board of Directors and entered as Item 6 in the minutes of that meeting, as tabled by that Board, was called from the table for further review. The item was briefly reviewed and the following action resulted.

Action: Upon motion, duly seconded, the Board of Directors approved that the Membership Committee be authorized to expend an additional amount up to $5000 over the amount allotted in the 1952–53 budget, in order to carry out the membership advertising program as recommended.

6. 1953 Exposition Committee

H. E. Rockefeller recommended that the 1953 Exposition Committee membership be expanded to include representative membership from the Manufacturers Committee, and further, that a Chairman be appointed for this Committee which Committee, on the basis of present standing, is without benefit of Chairman.

Action: Upon motion, duly seconded, the Chair was empowered to appoint new membership and to appoint a Chairman for the 1953 Exposition Committee.

Chairman Plummer extended invitation to Mr. Rockefeller to serve as Chairman of the Committee. However, Mr. Rockefeller declined and advised his Committee that he would, at a later date, offer recommendations to Mr. Plummer for approval and appointment.

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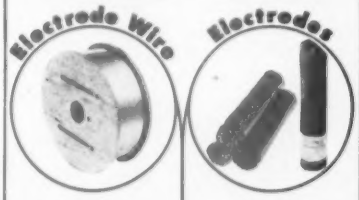

INERT GAS WELDING	MANUAL ARC WELDING
ALUMINUM	
Weldspool 43S	Weldbest 43S
Weldspool 2S	Weldbest 2S
Weldspool 52S	Weldbest 52S
TITANIUM	
Weldspool 930	
STAINLESS	
Weldspool 304 ELC	Weldbest 307
Weldspool 308	Weldbest 308
Weldspool 309	Weldbest 309
Weldspool 309 Cb	Weldbest 310
Weldspool 310	Weldbest 316
Weldspool 316	Weldbest 330
Weldspool 321	Weldbest 347
Weldspool 347	Weldbest 349
Weldspool 349	
STRAIGHT CHROMIUM STEELS	
Weldspool 405	Weldbest 410
Weldspool 410	Weldbest 430
Weldspool 420	Weldbest 442
Weldspool 430	Weldbest 446
	Weldbest 501
	Weldbest 502
LOW ALLOY STEELS	
Weldspool 70000	Weldbest 90
Weldspool 90000	Weldbest 100
Weldspool 120000	Weldbest 230
	Weldbest 260
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	Weldbest 139 (14% Mn)
NON FERROUS ALLOYS	
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Weldspool 610-SiBr	Weldbest 620C PBr
Weldspool 620-PBr	Weldbest 730 CuNi
Weldspool 630-AlBr	Weldbest 760 Cast Iron
Weldspool 730-CuNi	Weldbest 770 Nomel
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7. Meeting Sessions Chairmanship

J. R. Stitt, under the call for New Business, recommended to the Board that the National Program Committee be instructed to appoint only members of AWS to Chairmanship of technical sessions at its National Meetings. He cited as one example a Chairman who had on and off been a member of the WELDING SOCIETY and who had been serving as Chairman. He felt that the honor of being Chairman of these sessions should be confined only to members of the SOCIETY.

Ben Allen, Mosher Steel Co., Dallas gets bronze plaque from I. Morrison. Henry Neitzel Award for largest gain in membership

Action: Upon motion, duly seconded, the Board of Directors recommended to the Chairman of the National Program Committee that only AWS members be invited to serve as Chairmen or Co-Chairmen of technical sessions except where the situation makes desirable the services of one who is not a member of the SOCIETY.

8. Appreciation Letter

Chairman Plummer called for a motion authorizing Past-President Fraser and Secretary Magrath to draft a letter from the Board of Directors to retiring President Jennings, expressing the appreciation of the SOCIETY for his services during the 1951–52 year as President and Chairman of the Board of Directors.

Action: Upon motion, duly seconded, the foregoing recommendation was approved.

Adjournment: The meeting was adjourned at 5:45 P.M.

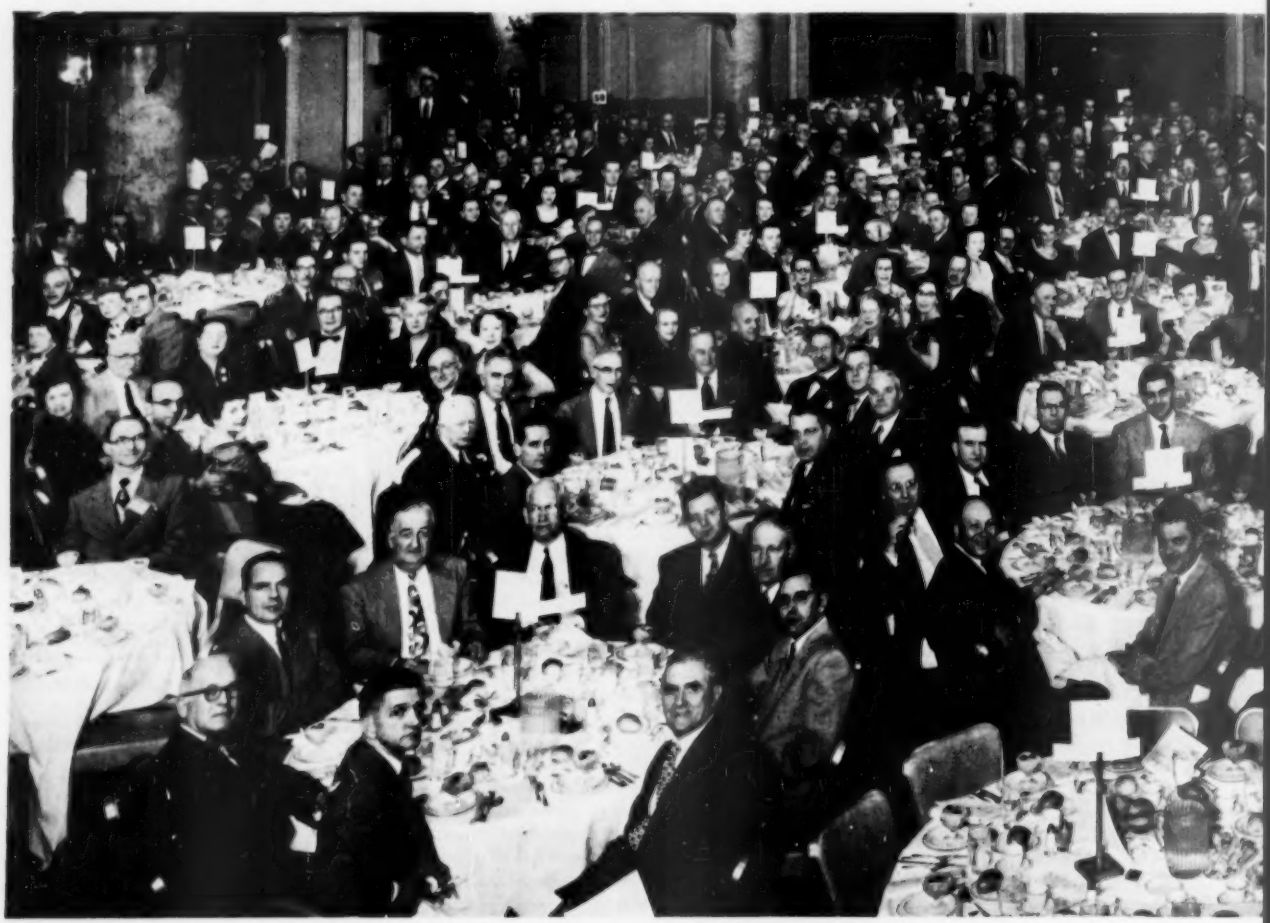
Philadelphia Is Host to 33rd National Fall Meeting

By W. Whitley

The Philadelphia Section of the AMERICAN WELDING SOCIETY experienced a most interesting and enjoyable task prior to and during the week of October 19–24th this year, playing host to the 33rd annual Fall Meeting. Under the able direction of R. Guenzel, Chairman of the Philadelphia Section, assignments were given to many of the willing and deserving officers and members of the local section who in turn performed their duties in a most fitting manner in the City of Brotherly Love.

Since the over-all program of this Fall Meeting involved most all of the AWS Sections, sincere gratification is hereby acknowledged. We mention with pride the many authors who presented technical papers. Should there be more awards of recognition given to authors of technical papers? If the many excellent papers given at this meeting are any indication, then surely consideration should be given this question.

When a large convention comes to a large city such as Philadelphia, a matter of housing presents a problem, but thanks to Louis Hauser, Chairman of the Housing Committee, this problem was well solved to the satisfaction and comfort of the many visitors to our city. There is another important consideration that is closely re-



lated to housing and that is getting acquainted, getting registered and being made welcome in the City of Brotherly Love. This pleasant assignment was given to Tom Berg, Chairman of the Hospitality Committee, who performed this duty splendidly along with his committee men.

The President's Reception held in the Burgundy Room of the Bellevue-Stratford Hotel was a huge success and a most enjoyable occasion for all officers, members and guests who were in attendance; thanks to John Lang and his Reception Committee since they were the men who saw to it that Philadelphia lived up to its warm tradition. It was observed that many friends from near and far were able to get together at this social hour, and toast to the memories of the past. The big question is—"where did John get such big jugs to fill so many cocktail glasses?"

The annual dinner held in the Ballroom of the Bellevue-Stratford Hotel was one of the gayest highlights of the week. With more than 480 in attendance at this dinner, there was every indication that all in attendance were thoroughly enjoying the occasion. Harry Pierce, Master of Ceremonies, was accompanied at the head table by the following honored guests: R. D. Bradway, O. B. J. Fraser, C. B. Voldrich, F. J. Mooney, C. A. Adams, G. O. Hoglund, H. R. Morrison, C. H. Jennings, R. A. Guenzel, J. G. Magrath, F. L. Plummer, E. R. Seabloom, R. D. Thomas, Jr., J. H. Humberstone, K. Wm. Ostrom and A. M. Garcia. After a brief welcoming address by Mr. Pierce, a most delicious roast sirloin of beef dinner was served piping hot with all the trimmings. Following this lovely dinner came one of the most refreshing and enjoyable stage shows that has ever been presented in the Philadelphia area. Among this fine cast of entertainers were Frankie Schluth, comedian and master of ceremonies; the Sulandias with their topical satires in dancing and "Ingaborg" (novelty illusion); Ned Smith and June Sayer, sweethearts of song, with their repertoire of best loved American aires; Ruth Daye, musical novelty; Prince Mendez, the Royal Deceiver; and Art Jordan's popular orchestra who accompanied the entertainment. Joe Jovinelly played incidental music during the dinner.

This dinner and entertainment program required the most careful planning with details in keeping with good taste for a large number of people. The man with the initiative and ability for this assignment was amply found in K. Wm. Ostrom, Chairman of the Annual Dinner Committee. Mr. Ostrom was ably assisted by L. D. T. Berg and J. E. Norcross. The writer is at a loss to find words that would truly express the gratitude and commendation due Mr. Ostrom and his committee for directing such a fine program.

Mike Eddins gets bronze plaque for Toledo Section's greatest percentage gain in members

Snapshots taken at Annual Fall Meeting

Scenes at President's Reception National Meeting Philadelphia, Oct. 20, 1952. Center picture Secretary Emeritus M. M. Kelly reminisces with Editor and Mrs. Spraragen.

Dr. C. A. Adams and Adams Lecturer C. B. Voldrich

J. F. Lincoln

Left to right—W. Spraragen, Fred Plummer, H. C. Boardman, G. O. Hoglund

LIST OF NEW MEMBERS

Effective November 1, 1952

ARIZONA

Baum, Paul Vernon (C)
Rousseau, Keith B. (B)
Schmidt, Paul (C)
Thude, Harry D. (C)
Wadsworth, Roland, Jr. (C)

ATLANTA

Harwood, W. E. (C)

BOSTON

Bourque, Edward F. (B)
Bullock, Hemenway R. (B)
Cantor, Michael (C)
Charlton, Webster J. (B)
Foster, George T. (B)
Lawrence, Roy I. (C)
Lindsay, James T. (B)
Swanson, Alva (B)

CHICAGO

Koch, Otto (B)
Lemberg, Howard A. (C)
Meyers, George N. (C)
Silver, Irv. (B)
Stears, Roy J. (B)

CLEVELAND

Black, Joseph R. (C)
Coleman, Vincent R. (C)
Glick, Ernest (C)
Krizan, Daniel (C)
Regn, Paul M. (C)
Ries, Gerald D. (B)
Robitaille, Martial J. (C)
Schutte, Walter (B)
Wallen, Alford L. (C)

COLUMBUS

Briner, Charles (C)
Campbell, Richard J. (D)
Shelley, David (D)
Sopher, R. P. (C)

DALLAS

O'Rourke, T. J. (B)
Somers, Kenneth E. (B)

DAYTON

Aiken, Leroy William (C)
Brown, Donald (C)
Hobart, William H., Jr. (C)
Jarvis, John H. (C)
Kadel, Albert (C)
McMahan, Paul D. (C)
Merriman, Joseph D (C)
Pietro, Anthony (C)
Schober, William R. (C)

DETROIT

Aikens, Allen (C)
Breen, M. J. (B)
Graham, John E. (C)
Hill, P. R. (C)
Kobeila, John (C)
MacIndoe, Thomas Hunter (B)
Weiss, Melvin (C)

HARTFORD

Lorence, Michael (B)

HOUSTON

Block, Edwin (C)
Bowen, C. M. (C)
Hodgeman, Herbert H. (B)
McBee, Frank W., Jr. (B)
Pringle, Roy P. (C)
Ragone, Louis A., Jr. (C)

INDIANA

Costello, Harold J. (B)
Garrett, Joseph R. (B)

IOWA

Beemer, Roger D. (C)
Greene, Fred R. (C)

FLOWLINE
WELDING FITTINGS
Stainless Steels, Monel, Nickel
BRING ALL THE ADVANTAGES
OF WELDED CONSTRUCTION
TO USERS OF
CORROSION-RESISTANT PIPING . . .
More than 3000 Stock Items
FLOWLINE Welding Fittings are stocked throughout the country by leading warehouses specializing in corrosion-resistant metals, in Schedules 10, 40, and 80 (Schedule 5 also available)—sizes ½ inch through 12 inches—in stainless steel Types 304, 316, and 347; Monel, Nickel, and Aluminum.
With FLOWLINE Fittings it is practicable for users of corrosion piping to obtain the fullest economy and efficiency of simplified welded construction—in design, installation, operation, and maintenance.
Welded construction provides greatest freedom in designing the layout. The system uses less space, is lighter in weight, and can be assembled and put into service faster. It is permanently leak-proof, has greater strength, longer life, and best flow characteristics.
FLOWLINE corrosion-resistant fittings include tees, stub ends, ells, reducers, and caps. All are made of forged or wrought metals, cold formed by our exclusive process and annealed, putting the metal in the best condition for corrosion service.
WELDING FITTINGS CORP.
NEW CASTLE, PENNSYLVANIA
World's Largest Manufacturer of Stainless Welding Fittings

Nau, John O. (C)
Novy, Russell F. (C)
Sudtelgte, R. L. (C)

IOWA-ILLINOIS

Allen, James E. (B)
Bargmann, Waldo O. (C)
Best, Floyd (B)
Dowsett, William J., Jr. (B)
Escobar, Ernest A. (C)
Fetter, Sam (B)
Fosdyck, E. C. (C)
Gentrey, H. B. (C)
Hanson, J. S. (C)
Harding, W. J. (C)
Heil, C. W. (C)
Krier, Earl (B)
Kruse, Howard H. (B)
Landry, Norman E. (B)
Lundberg, Arlo G. (B)
Mayer, Wilbur (C)
Musselman, C. E. (C)
Page, Lloyd (C)
Peterson, Willard A. (C)
Roba, Roy (B)
Saffell, Marion W. (B)
Sandberg, Raymond S. (C)
Schmidt, Russell A. (B)
VanVooren, Fred A. (B)
Vazquez, James C. (C)
Wessell, John J. (B)
White, W. M. (B)
Zippay, George J. (C)

KANSAS CITY

Blythe, William F. (B)
Henry, Tom (C)
Oberembt, Richard J. (B)
Paterson, C. S. (C)
Sprowls, Delmar V. (B)

LEHIGH VALLEY

Hetrick, Leonard F. (B)
Kunsman, Winfield (B)
Oyler, Glenn W. (C)

LONG BEACH

Peterson, Randolph M. (C)

LOS ANGELES

Beatty, William R. (B)
D'Amico, C. Donald (B)
Hermann, Joe V. (C)
Marr, Ernest (C)
Pardoe, Albert R. (C)
Russnak, Clarence J. (B)
Thompson, Lloyd C. (B)
Watts, Louie K. (B)

LOUISVILLE

Behrman, C. O. (B)
Brooks, Barlow W. (B)

MAHONING VALLEY

Olemik, Edward A. (B)

MARYLAND

Meyer, Harry H. (B)
Ray, James B. (C)

MICHIANA

Bushman, William S. (C)
Fairchild, Clifford L. (C)
Hartel, E. Richard (C)
Pendl, Gene R. (C)

NEW JERSEY

Parker, W. J. (B)

NEW YORK

Brooks, Mortimer (B)
Hamilton, Samuel H. (B)
Thielsch, Helmut (B)
Vanderpool, Howard (B)
Wendover, William E. (C)

NIAGARA FRONTIER

Kemp, W. Gordon (B)
Reinhardt, Gustav (C)
Willing, Fred E. (B)

NORTHERN NEW YORK

Armstrong, Robert E. (B)
Baehner, Fred A. (C)
Marvin, Joseph M. (C)
Mathias, Walter J. (C)
Nelson, Robert L. (B)
Petryck, Leon M. (D)
Rzant, Adolph William (B)
Savage, Warren F. (B)
Schaaf, Bernard W., Jr. (D)
Suzuki, Haruyoshi (D)
Verchere, Louis (D)

NORTHWEST

Brakke, H. E. (C)
Huber, William M. (C)

NORTHWESTERN PA.

Boyer, Kenneth R. (C)
Burns, Paul L. (C)

OKLAHOMA CITY

Alden, John James (B)

PASCAGOULA

Collins, Percy E. (C)
Doescher, E. H. (C)
Ezell, Jesse L. (C)
Greenough, William Otto (C)
Hanby, D. A (C)
Nichols, Theodore F. (C)
Partin, J. N. (C)
Rigby, George D. (C)
Sandner, Frank X., Jr. (B)
Simmons, R. C. (C)
Slaven, F. W., Captain (C)
Sweeney, James A. (B)
Tipton, Edward J. (C)
White, Cecil (C)

PHILADELPHIA

Poole, Lorin K. (C)

PITTSBURGH

Goetz, Roland W. (B)
Hazen, Frank D (B)
Klasson, George A. (C)
Marr, Charles W. (C)
Turzak, Joseph J. (B)

PUGET SOUND

Dunsmoor, G. E. (C)

ROCHESTER

Carlin, Frank (C)
Lienhart, Wilfred Raymond (C)

SAGINAW VALLEY

Rupp, A. G. (C)

ST. LOUIS

Kirsch, Leo J. (C)
Lindecke, Fred C. (C)
Randall, Robert E. (B)

SALT LAKE CITY

Bringhurst, George S. (B)

SAN FRANCISCO

Naillon, J. D. (C)
Smith, Glen D. (C)

SANGAMON VALLEY

Akrens, Edward C. (C)
Anderson, H. E. (C)
Baugh, Lester L. (B)
Beck, John D. (B)
Boehm, Francis (B)
Bowman, Clarence A. (B)
Boyer, J. Parke (B)
Brown, James D. (C)
Burnett, Harold M. (C)
Cochran, Henry W. (B)
Constant, Clarke J. (B)
Damon, Les (B)
Dawson, C. W. (C)
Earley, Charles L. (B)
Fathauer, Herbert H. (C)
Finney, Gene (B)
Fitzjarrald, Lawrence (B)
Glacken, Joseph (C)
Gneckow, J. C. (C)
Graham, Carl (B)
Gregory, Joe (C)
Grolla, Irvin (C)
Hanks, J. D. (B)
Hamill, E. C. (C)
Herbst, C. H. (B)
Hertel, Jerome (B)
Hirstein, Marvin O. (B)
Holinga, John B. (C)
Huff, Charles (B)
Kopetz, Robert W. (B)
Lee, George (B)
LeVeque, Charles (C)
Lewis, John R. (C)
Lewis, Russell I. (B)
Mallow, Paul E. (B)
McDonald, E. C. (C)
Meyer, Carl F. (C)
Miller, Clarence E. (C)
Moore, F. E. (C)
Osborne, Clyde P. (C)
Overbeck, Carl M. (C)
Peters, E. Donald (B)
Remmert, William A. (C)
Reynolds, Stanley L. (C)
Richards, Lee R. (C)
Richardson, Lloyd F., Jr. (B)
Richardson, W. L. (B)
Robertson, Robert D. (C)
Robinson, Laurance P., Jr. (C)
Scherer, Carl E. (C)
Scribner, Roy S. (C)
Seman, J. J. (C)
Shaw, Carl H. (B)
Sheehan, Gerald A. (C)
Smith, Haskell (B)
Smith, LeRoy H. (B)
Spires, William E. (C)
Stogsdill, James C (C)
Stolle, Leroy E. (B)
Stowe, Talmadge M. (C)
Swigart, Robert (C)
Swinford, Lester (B)
Thoman, John A. (C)
Thomassen, Ray (C)
Tranquilli, Armand J. (C)
Walker, Cecil L. (C)
Wallace, Myrel, Sr. (B)
Wallace, Myrel, Jr. (B)
Warner, C. J. (B)
White, John A. (C)
Wild, Kent D. (C)
Wilhelmy, Arthur F., Sr. (B)
Wilson, H. B. (B)
Yates, Carl S. (C)

SYRACUSE

Atkinson, Ross B. (C)
Gloska, Edward J. (C)
Humbert, John C. (C)
Hurst, Robert C. (C)
Klix, Hugo C. (C)
Schramm, Carl P. (C)

TOLEDO

Didion, Ralph J. (C)

TULSA

Hammond, Robert A. (B)
Hartley, Vernon H. (B)
Sariego, W. G. (B)

WESTERN MASS.

Bishop, Kenneth E. (B)
Mission, Arthur (B)

WESTERN MICHIGAN

Crandall, G. A. (B)
Guenther, Eril (C)
Moelk, Arnold O. (B)

WICHITA

Boring, Donn (C)
Kappler, Karl (D)
Klepenger, G. C. (C)

WORCESTER

Blackwell, W. G. (B)
Burnham, Bruce H. (B)
Cederholm, Frank H. (B)
Goehring, Bernhard H. (B)
Harts, Walfrid (C)
Holmberg, Harry (C)
Hook, Russell P. (B)
Johnson, Maitland R. (C)
Johnson, Walter G. (B)
Schofield, Carl D. (B)

YORK-CENTRAL PA.

Lilly, W. W., Jr. (B)
Rutter, H. Paul. (C)

NOT IN SECTIONS

Bolton, Manuel D. (B)
Frias, German Alberto (B)
Onishi, Iwao (B)
Rogers, Luther P. (B)
Roland, Serge (B)
St. Denis, Stuart (B)

Members Reclassified

During the month of November

HOUSTON

Reeves, Henry A. (D to C)

LONG BEACH

Walling, Albert Shell (C to B)

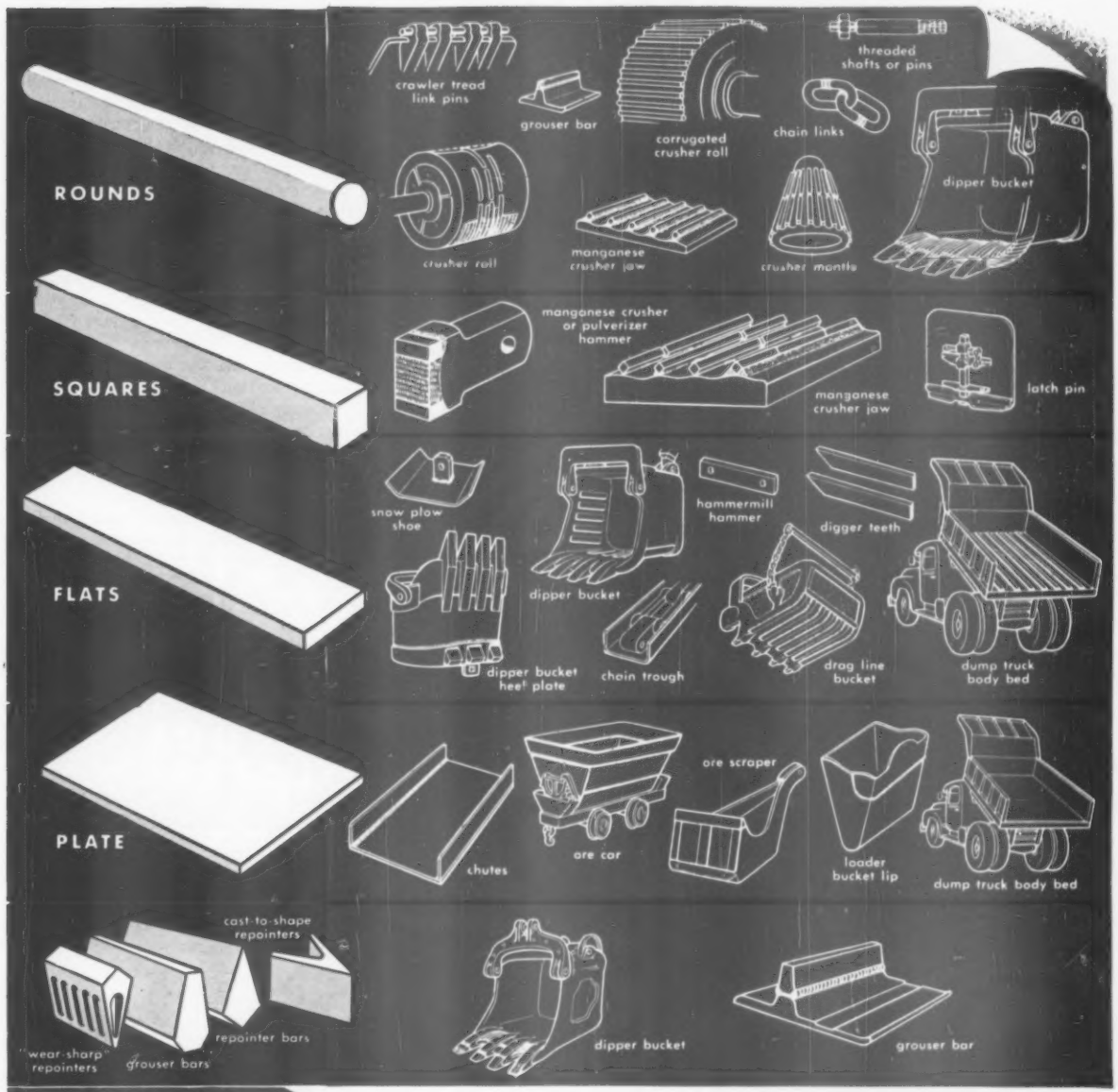
LOS ANGELES

Wheeler, Howard (D to C)

NORTHWEST

Rose, Rolland H. (D to C)

AMSCO MANGANESE STEEL WELDMENTS
ADD NEW LIFE TO WORN EQUIPMENT



Amsco Welding Rods and Electrodes For repair welding of manganese steel shapes to worn equipment, American Manganese Steel Electrodes retain their toughness and give real operating savings.

Amscoating with Amsco Hardfacing Rods increases service life . . . reduces shutdowns.

Contact your Amsco Distributor or write for illustrated catalog WA-77 on Amsco Manganese Steel Weldments and Hardfacing Selector Guide.

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399 EAST 14th STREET • CHICAGO HEIGHTS, ILL.

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Amsco Welding Products distributed in Canada by Canadian Liquid Air Co., Ltd.

$2250 in Prizes

FOR PAPERS ON RESISTANCE WELDING

Contest Closes July 31, 1953

PRIZES AND CONTEST RULES

Announcement has been made of cash prizes to be awarded in 1953 by the Resistance Welder Manufacturers' Assn. for outstanding papers dealing with resistance welding subjects. The total amount of the awards is $2250, and a wide choice in subject matter is allowed in order to assure eligibility to all papers which cover worth-while and significant achievements in the field. The contest judges will be appointed by the AMERICAN WELDING SOCIETY, and awards will be made at the 1953 Fall meeting of the SOCIETY. The prizes and rules governing the contest are as follows:

Industrial Source. One prize of $750 for the best paper emanating from an industrial source, consulting engineer, private or government laboratory, or the like, the subject matter of which is concerned specifically with resistance welding. There are no restrictions on the scope of the subject matter. For example, it may be devoted to redesign of a product or products for resistance welding, improvement (from a welding and cost viewpoint) in a present design for resistance welding, resistance welding research, development of new procedures to broaden the field of application of resistance welding, etc. Papers in these categories should explain the economic importance of the accomplishments described—that is, cost savings, production improvement, scope of application, etc.

A prize of $500 for the second best paper in the above classification. A prize of $250 for the third best paper in the above classification.

University Staff Source. A prize of $300 for the paper emanating from a University source (the author of which is either an instructor, graduate student or research fellow) which in the opinion of the Jury of Award is the greatest original contribution to the advancement and use of resistance welding from this source.

A prize of $200 for the second best paper in the above classification.

University Undergraduate Source. A prize of $250 for the paper emanating from a University source (the author of which is an undergraduate student) which in the opinion of the Jury of Award is the greatest contribution to the advancement of resistance welding from this source.

While each prize award is the total for the paper, each individual author and co-author, if any, will be presented with a suitable certificate.

The contest is open to anyone, without restriction, from the United States, its possessions and Canada. It is also open to any member of the AMERICAN WELDING SOCIETY in any grade from any place in the world. The contest is considered as having opened Aug. 1, 1952. In order to be eligible for this contest all papers must be forwarded so as to be delivered at AMERICAN WELDING SOCIETY headquarters no later than 5:00 P.M., July 31, 1953. Papers which are to be presented at the Annual Meeting of the AMERICAN WELDING SOCIETY in October may also be entered in this contest, in which case a draft or copy of the paper must be filed with the AMERICAN WELDING SOCIETY not later than July 31, 1953. To be eligible, author must indicate paper has not been considered previously in the contest, or shall not have been published prior to its appearance in THE WELDING JOURNAL, except in a school paper in the case of a paper from a University source. Authors should indicate on the title page the class in which their paper is entered, i.e., "Industry Class," "University Staff Class" or "University Undergraduate Class."

All papers submitted in the contest become the joint property of the RWMA and the AMERICAN WELDING SOCIETY, who will retain all rights thereto. The AMERICAN WELDING SOCIETY will appoint five judges, who will judge the relative merits of the various papers submitted and make the awards accordingly. The decison of the judges will be final.

For the author or authors to be eligible for this award, the paper shall describe clearly original work done by them or under their supervision on resistance welding in any of its aspects by any method or process. The paper shall be a full disclosure of the subject. The paper shall contain no statement which is unethical advertising or sales promotion. The paper may contain statements of fact, including the names of either individuals or organizations of any kind, commercial designations, trade names, etc. The minimum length requirement is 2500 words.

All papers must be typewritten, double spacing, written on one side only of blank white paper. Photographs, charts, graphs, etc., may either be attached directly to the copy or may be detached, in which case they should be clearly identified with figure numbers, captions, etc.

Papers entered in this contest should be sent to AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y. If mailed to arrive no later than July 1, 1953, three copies should be furnished. If mailed to arrive between July 1, and July 31, 1953, six copies should be furnished.

"Shortstub" ELECTRODE HOLDERS

Convert the total fluxed portion of all electrodes into weld metal —REDUCE STUB WASTE TO THE LOWEST POSSIBLE

TWO SIZES FILL ALL NEEDS

Model	Length Inches	Wt. Ozs.	Cap. Amps.	ELECTRODE Size
No. 4	8⅞"	17	400	1/16" to ¼"
No. 6	10⅞"	24	600	3-32" to ⅜"

Quantity Prices 10 to 20% less *Write for FREE 16 Page Booklet FORM SS-3.*

Mfg. by
BERNARD WELDING EQUIPMENT CO.
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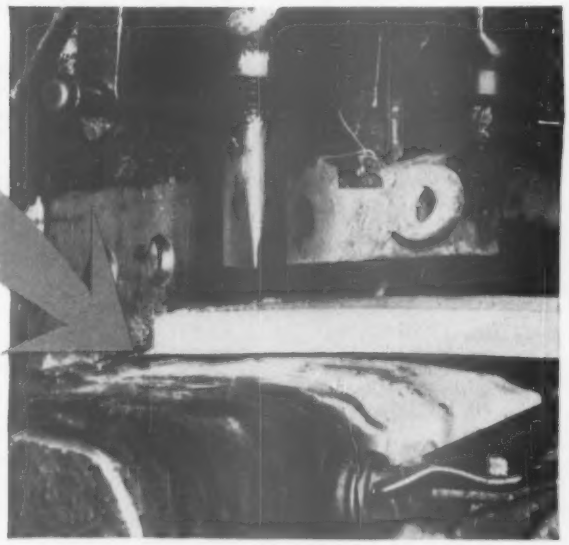
HOT SHEAR BLADES STAY SHARP...when Hard-Faced with HASTELLOY Alloy C

TRADE-MARK

A 3/32-in. deposit of HASTELLOY alloy C on the cutting edge of this shear blade has increased its life by four times. The edge can be rebuilt again and again, when it finally does wear.



This shear blade is used in a plant producing tough non-ferrous alloys. Before hard-facing was adopted, the blades would chip, lose their edge, and have to be scrapped after shearing only about 50 tons of metal. They now can handle roughly four times that amount of metal because they are protected with HASTELLOY alloy C. They can be machined and hard-faced again when they finally do wear. One set of hard-faced blades has been in use in the plant for more than three years with periodic maintenance.

In steel mills, too, HASTELLOY alloy C has increased the life of blooming mill shears by as much as 10 times. Hard-faced blades have lasted 110 turns without maintenance.

HASTELLOY alloy C rod has also been applied to many other hot-working parts with outstanding success. The metal flows well by metallic arc welding or HELIARC welding without preheating. No peening is necessary. Deposits of HASTELLOY C work-harden in service. They can be machined by conventional methods.

For information on how to apply HASTELLOY alloy C to hot-working parts, write for a copy of "HAYNES Hard-Facing Manual." For on-the-job help in applying the rod, get in touch with the nearest District office.

HAYNES alloys

TRADE-MARK

"Haynes," "Hastelloy" and "Heliarc" are trade-marks of Union Carbide and Carbon Corporation.

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A Division of
Union Carbide and Carbon Corporation
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General Offices and Works, Kokomo, Indiana
Sales Offices
Chicago — Cleveland — Detroit — Houston
Los Angeles — New York — San Francisco — Tulsa

NEWS OF THE INDUSTRY

Tempil° Distributor

The Welding Products Division of the A. O. Smith Corp., Milwaukee, Wis., have become distributors for Tempil° Corp. line of temperature indicating products—Tempilstiks°, Tempilaq° and Tempil° Pellets.

Weldwire Announces Plant Expansion

In order to meet demands for precision layer-level wound welding wire and coated arc welding electrodes, the Weldwire Co., Inc., has acquired additional plant space at its present location at Emerald and Hagert Sts., Philadelphia 25, Pa. According to J. S. Kobler, president, this expansion will enable Weldwire to increase production in the specialized spooling of pure aluminum, aluminum alloys, stainless steels and specialty steels for the inert-gas-shielded arc-welding processes. Part of this space will be used to increase the manufacture of all grades of stainless and specialty coated electrodes for manual arc welding.

Casting Film

The Lebanon Steel Foundry of Lebanon, Pa., has announced that prints of its moving picture, "Steel with a Thousand Qualities," have been ordered by the Mutual Security Agency for showing abroad in MSA nations. The announcement was made by William H. Worrilow, president of the foundry. The film depicts methods used by modern American foundries in casting metals ranging from valve casings to stainless steel components for jet aircraft engines. The film is in color and runs for approximately 37 minutes.

Independent Oxygen Manufacturers' Assn.

The Independent Oxygen Manufacturers' Assn. held their Annual Fall Business Meeting at the Edgewater Beach Hotel, Chicago, Ill., on November 10th, 11th and 12th. Numerous business sessions were held during the course of the meeting pertaining to problems of the industry. The following officers and Board members were elected: *President*, Marvin White, Welding Gas Products Co,. Chattanooga, Tenn.; *Vice-President*, Harold Macauley, Wolverine Gas Products, Inc., Saginaw, Mich.; *Secretary*, J. F. Wagner, The Burdett Oxygen Co., Inc., Cleveland, Ohio; *Treasurer*, R. L. Swope, Southern Oxygen Co., Bladensburg, Md. *Board of Directors:* Walter Brant, Indiana Oxygen Co., Indianapolis, Ind.; G. Morgan McBride, Western Oxygen, Inc., Seattle, Wash.; Tom Kean, Sierra Oxygen Co., Reno, Nev.

Baltimore Welding Metallurgy Course

A six-lecture course on the welding metallurgy of alloy and stainless steels was recently sponsored by and held at the showrooms of Earlbeck & Landrum, Inc., Baltimore-Washington dealer for The Lincoln Electric Co. Sessions were held during November twice weekly from 8 to 10 P.M. and were attended by 40 men. The instructor was George E. Linnert, Welding Engineer of Armco Steel Corp., who recently completed the revision of the second edition of *Welding Metallurgy* published by the AMERICAN WELDING SOCIETY.

Literature and graphs were given to the students and slides were extensively used in the course presentation. Included among the students were plant superintendents, welding Engineers, technicians and welders from many of the large users of arc welding in the Baltimore area.

All who attended agreed that Mr. Linnert did an outstanding job of imparting a maximum of welding know-how in a minimum of time. Evidence of their high interest is shown by the photograph below of the class taken on election night, Nov. 4, 1952, when 31 men were in attendance. A second follow-up series is now being planned for the coming spring.

Scaife 150th Birthday

Scaife Co., oldest manufacturing firm in the United States west of the Allegheny mountains, is celebrating its 150th anniversary this year. But no fanfare marks this important milestone; a brief, simple program, held November 20th, formally opened the new Scaife office building in Oakmont, Pa.

An "open house" for employees and their families marked the occasion, according to Alan M. Scaife, board chairman of the steel-fabricating company which has grown in father-and-son tradition through five generations of the Scaife family.

Founder of the Company was Jeffery Scaife, an industrious young maker of tinware and japanned ware, in 1802—when Pittsburgh was a little outpost of jerry-built cabins and mud streets, barely free of Indian raids, and the War of 1812 was still a decade in the future.

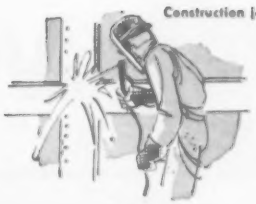
Although never an industrial giant, the once-tiny business begun by Jeffery Scaife grew solidly through 15 decades, surviving fires and floods, panics and wars, and the changing fortunes of the market place.

The company has been engaged throughout its history in fabricating metals. It has been identified constantly with the growth of the nation, and the nation's greatest industrial city. But Scaife Co. was always quick to adapt itself to new public demands for products. It pioneered the familiar housefold range boiler many decades ago. In recent years Scaife has developed remarkable leadership in the development and production of pressure vessels and drawn shapes—among them cylinders for liquefied petroleum, refrigerant and acetylene gases, tanks for compressed air, water storage and other tanks for the plumbing trade.

When the nation faces trouble, Scaife takes up arms with vengeance. In 1812 it

Baltimore Welding Metallurgy Course

A.O.Smith Field-King



The gas engine driven welder with the features YOU wanted!

All-electric dual control provides continuous, stepless current adjustment with constant engine speed!

Electric idling control, unlike other types which can easily clog or get out of order, assures perfect adjustment at all times and practically no maintenance!

Auxiliary D. C. power is a full 2½ kilowatts, the largest available in any standard engine-driven machine!

A rugged machine with less bulk! A field welder with 25% extra horsepower that guarantees smoother, easier welding!

Lighter weight! The Field King 200-Amp. model, for example, weighs only 985 pounds. This is achieved by modern torsional mounting. This lighter weight means greater mobility and lower shipping costs.

Greater fuel capacity! The larger gas tank means a full nine-hour working day without stopping to refuel.

A safer machine! Extra baffle plates on each side of the gas tank prevent accidental fires caused by unintentionally bumping the electrode against the gas tank.

It's the field welder you've been asking for . . . now available with the A. O. Smith name, guarantee and reputation behind it.

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Welding Products Division
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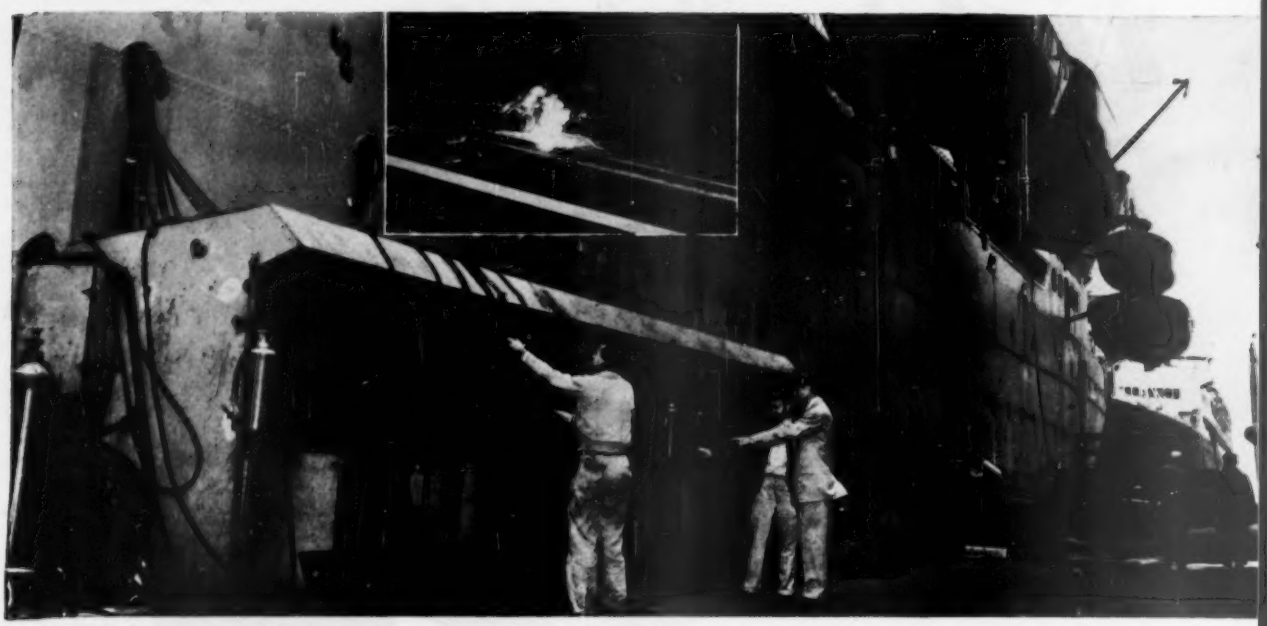
Welding Arcs



OLD G-E WELDERS? Some have been in this salt air for ten years but they still give top performance. Through the porthole a workman feeds cable to weldors (inset) deep in the hold—sometimes as far away as 300 feet. Don't miss the new G-E a-c sets.

DEPENDABLE G-E A-C WELDERS HELP SPEED U.S. SHIPBUILDING PROGRAM

On the piers and in the shops of the nation's leading shipyards you will see a-c welding used on nearly every job.

The many corners and the flat and horizontal joints to be welded in ship construction make a-c the best method for most applications. The change from d-c to a-c welding has saved these yards hundreds of thousands of dollars.

Geared for 24-hour work on ships when fast construction or repair is necessary, these yards recognize the advantages of speedy welding with G-E a-c sets.

FAST LOCAL SERVICE is available to each yard on welding problems through the nearby G-E Welding Distributor. You, too, can get this *local* service, backed by the research, engineering and service facilities of General Electric.

When you make your swing to a-c welding, swing to G.E., pioneer in a-c welding and still the leader. For equipment, electrodes, accessories, spare parts and service, call the local G-E Welding Distributor—see list next page, or look in the yellow pages of your telephone book under "Welding Equipment—General Electric." General Electric Co., Schenectady, N. Y. 710-1

GENERAL ELECTRIC

News

information on equipment, electrodes and accessories from G.E., pioneer in a-c welding

HERE'S THE A-C SET YOU ASKED FOR

Built for dependability and easier welding . . . on suggestions from G-E customers

1. Fast range-changing with new switch—no leads to unplug, no connections to make.

2. Quick inspection is easy. Remove the entire case in two minutes without disconnecting the primary leads.

3. You save on power with a case-mounted primary switch for turning off the current between welds.

4. Fast take-offs—no scratching or pecking. Weldors save time because automatic hotstart adjusts instantly to every setting.

5. Smooth arc is easy to control because arc-stabilizing capacitors eliminate popouts.

6. Case stays cool because the large fan gives every component adequate ventilation.

7. Pinpoint accuracy in current settings is easy with the wide, double current range.

8. Long coil life and trouble-free operation are built in with Class H Silicone insulation, able to take temperatures 30 per cent higher than ordinary insulations.

Copyright 1953, G.E. Company

Joe Magee says . . . Here's where to use low-hydrogen rods . . .

If you're looking for electrodes to weld high alloy steel, medium carbon steel and sulphur steel without underbead cracking and porosity, G-E W-32 low-hydrogen rods are your answer. These electrodes will weld equally well on a-c or d-c, and their high ductility and toughness will hold up exceptionally well at low temperatures. G-E W-32 is an excellent trouble-shooter for production welding, and it is ideal as a maintenance tool, too. Cost-wise, it is entirely practical to replace ordinary electrodes on some applications with G-E low-hydrogen electrodes which offer higher speeds and higher impact values.

The use of low-hydrogen electrodes is especially recommended for welding plate that must be enameled. The entrapped hydrogen resulting from the use of ordinary electrodes escapes after enameling and causes flaking. The use of G-E W-32 electrodes will practically eliminate this condition and save many hours of re-working.

ELECTRODE COMPARATOR!

Now it's easy to switch to G-E electrodes. AWS classification and competitors' numbers available for 23 G-E stainless steel and 36 regular G-E electrodes on sturdy pocket-size slide-rule.

Available *only* through your G-E Welding Distributor. Ask him for one today!

See your nearby Welding Distributor—

Alabama: Birmingham—Alabama Oxygen, Young & Vann Supply; Mobile—Turner Supply

Arizona: Phoenix—Consolidated Welding Supply

California: Fresno, Los Angeles, Oakland, Sacramento, San Diego, San Francisco, Ventura—Victor Equipment

Colorado: Boulder, Colorado Springs, Denver, Durango Ft. Collins, Ft. Morgan, Greeley, LaJunta, Longmont, Pueblo—Hendrie & Bolthoff

Florida: Ft. Pierce, Hollywood, Key West, Melbourne, Tampa, Vero Beach, West Palm Beach—Florida Gas & Chemical

Georgia: Atlanta, Macon—Welding Supply & Service; Augusta—Marks Oxygen & Welding Supply; Columbus—Williams Welding Supplies

Idaho: Boise—Olson Manufacturing

Illinois: Chicago, Moline, Morton—Machinery & Welder

Indiana: Evansville—Drill Master Supply; Ft. Wayne, Indianapolis, Richmond—Sutton-Garten; South Bend—Perry Welding Sales & Service

Iowa: Des Moines—Machinery & Welder

Kansas: Coffeyville—Thompson Bros. Supply & Welding Equip.; Hutchinson—Kopper Supply

Kentucky: Louisville—Reliable Welding; Paducah—Henry A. Petter Supply

Louisiana: Alexandria, Shreveport—Hughes Oxygen; New Iberia—Welders Supply; New Orleans—Equitable Equipment; Opelousas—Daigle Welding Supply, Lake Charles—Welders Equip.

Maryland: Baltimore—Arcway Equipment

Massachusetts: Boston—New England G-E Welding Sales Division

Michigan: Detroit—Welding Sales & Engineering; Grand Rapids—Miller Welding Supply

Minnesota: Duluth—W.P.&R.S. Mars; St. Paul—Production Materials

Mississippi: Jackson—Jackson Welding & Supply

Missouri: Kansas City—Hohenschild Welders Supply; St. Louis—Machinery & Welder

Montana: Billings—Valley Welders Supply; Butte, Great Falls—Montana Hardware

Nebraska: Lincoln—Lincoln Welding & Supply; Omaha—Baum Iron

New Jersey: Kenilworth—Welding Sales

New Mexico: Albuquerque—Industrial Supply City Hobbs—Western Oxygen; Las Cruces, Silver City—Car Parts Depot, Inc.

New York: Buffalo—Welding Equipment Sales; Syracuse—Welding Engineering & Equip.

North Carolina: Charlotte—Dixie Gases, Gastonia—Gastonia Motor Parts

North Dakota: Bismarck, Fargo—Acme Welding Supply; Fargo—Dakota Electric Supply

Ohio: Akron, Cincinnati, Cleveland, Columbus, Dayton, Mansfield—Burdett Oxygen; Toledo—Odland Iron Works

Oklahoma: Tulsa—G-E Welding Sales Division

Oregon: Eugene, Portland—J. E. Haseltine

Pennsylvania: Philadelphia, Pittsburgh—Arcway Equipment

South Carolina: Columbia, Greenville—Welding Gas Products

South Dakota: Deadwood—Hendrie & Bolthoff

Tennessee: Chattanooga, Knoxville, Nashville—Welding Gas Products; Memphis—Delta Oxygen

Texas: Abilene—M&M Welding Supply; Alice, Corpus Christi—Crane Welding Supply; Alpine, El Paso, Marfa, Pecos—Car Parts Depot; Amarillo—Tex-Air Gas; Brownsville, Harlingen—Acetylene Oxygen; Dallas—Hill Equipment & Supply; Houston—G-E Welding Sales Division; Lubbock—Welders Supply; Midland—West Texas Welders Supply; Odessa, Pecos—Western Oxygen; Plainview—Plains Welding Supply; San Angelo—Southwestern Welding Supply; Snyder—Western Welding Supply; Texarkana—Hughes Oxygen; Wichita Falls—Nortex Welding Supply

Utah: Salt Lake City—The Galigher Co.

Washington: Seattle, Spokane—J. F. Haseltine

West Virginia: Bluefield—Bluefield Supply; Charleston—Virginian Electric, Huntington, Logan—Logan-Hardware & Supply

Wisconsin: Milwaukee—Machinery & Welder

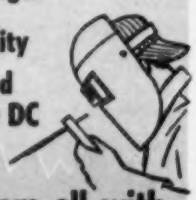
Alaska: Anchorage—Northern Supply

Canada: Toronto—Canadian G.E.

Hawaii: Honolulu—American Factors, Ltd.

If you do AC Bronze Welding, check these Advantages

- No snuff outs
- Easy striking
- Wear resistant
- Corrosion resistant
- Welds dissimilar metals
- High strength
- High ductility
- Can be used with AC or DC



You get them all with AMPCO-TRODE* 'AC' Coated Aluminum Bronze Electrodes

THESE Ampco-Trode "AC" advantages mean lower welding costs for you—better, faster jobs—less trouble. Besides the advantages listed you also get a smooth bead that feather-edges perfectly into the base metal — whether it's used on cast iron, malleable iron, bronzes, some nickel alloys, or dissimilar metals.

Moreover, Ampco-Trode "AC" beads are free from porosity and surface pitting. Spatter loss is low, too.

Ampco-Trode "AC" is available in 5 grades having Brinell hardness ranging from 135 to 300, tensiles from 77,000 to 90,000 psi, and yield strengths from 35,000 to 69,000 psi.

For additional information about versatile, money-saving Ampco-Trode "AC" electrodes call your nearby Ampco distributor or write us.

*Reg. U. S. Pat. Off.

AMPCO

AMPCO METAL, INC.
MILWAUKEE 46, WISCONSIN
West Coast Plant: Burbank, California
It's Production-Wise to Ampco-ize!

was tar buckets and sponge buckets—tar greased the cannon wheels, sponges cleaned the cannon for the next volley of fire. When North fought South in the 1860's the company, under William B. Scaife (the second generation head of the concern) turned out shell canister plates, shell straps, cantle plates for cavalry saddles.

During World War I Scaife Co. was a leading producer of Diesel engine starting tanks for submarines, mine sweepers and other Naval vessels, and flame thrower tanks for the Army.

In World War II it was 4.2 mortar shells, which the company proved could be made at great savings in both money and metal from standard hot-rolled tubing. This experiment—at Scaife's own expense—so convinced the Government that the company produced nearly two million of them for the Chemical Corps before the war's end.

The same kind of technical leadership, earned the hard way in producing peacetime goods, enabled Scaife to produce general-purpose aerial bombs at a lower cost than that of any other manufacturer in the country.

Within the past two years the company has performed a new kind of metal magic—developed its unique "reverse draw" method for deep drawing pressure vessels, shapes, tanks and containers. The complete facilities for this fabricating work at the modern Oakmont plant include large and expensive hydraulic presses. One 1500-ton giant is the largest of its kind in the world.

For the fighting in Korea, Scaife was called upon once more to manufacture high-explosive mortar shells, and is working with the Navy and the Air Force in development of special ordnance items. Armor plate also is being fabricated for tanks.

Many Scaife men represent second, third and even fourth generations of the company's employees—a fact in which they, and the Scaife management, understandably take pride.

Controller of National Welding Equipment Co.

With the ready approval of Hugh F. Brown, President, George L. Hammon [illegible], Vice-President and General Manager, announces the appointment of G. H. Dekker as Controller of the National Welding Equipment Co. Mr. Dekker takes over the responsibilities of E. R. Russell who is retiring after diligently and loyally serving National for more than a decade.

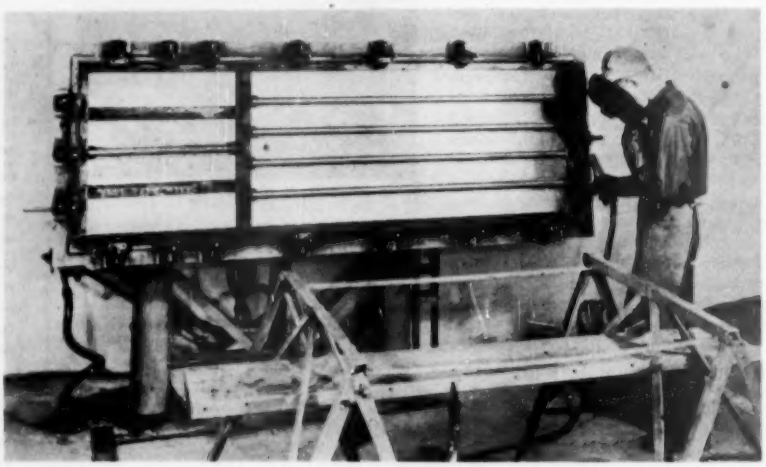
De-Sta-Co Toggle Clamps Adapted to Schieber Plug Weld Fixture

De-Sta-Co Toggle Clamps have been adapted to a plug weld fixture for metal-folding wall tables by the Schieber Manufacturing Co., Detroit, Mich. R. E. Schieber, president, stated that the De-Sta-Co clamps have boosted production, cutting labor costs to a minimum.

After the table top has been de-burred, it is placed on the hydraulic tilting table and the stainless band is clamped in place by the toggle clamps. By means of compressed air the table is tilted into position for the plug welding application. Nineteen No. 250 clamps and one No. 210 U clamp with a double hold bar are used to obtain constant pressure of the stainless steel band to the table tops. The clamps permit ease of insertion and removal of the steel table tops, which make them the ideal choice for this application.

The Schieber "In-wall" tables and benches permit economy in the utilization of space in schools, clubs, churches, community houses and industrial buildings. The tables are covered with heavy linoleum top surfaces.

The De-Sta-Co Toggle Clamps are manufactured by the Detroit Stamping Co., 322 Midland Ave., Detroit 3, Mich.

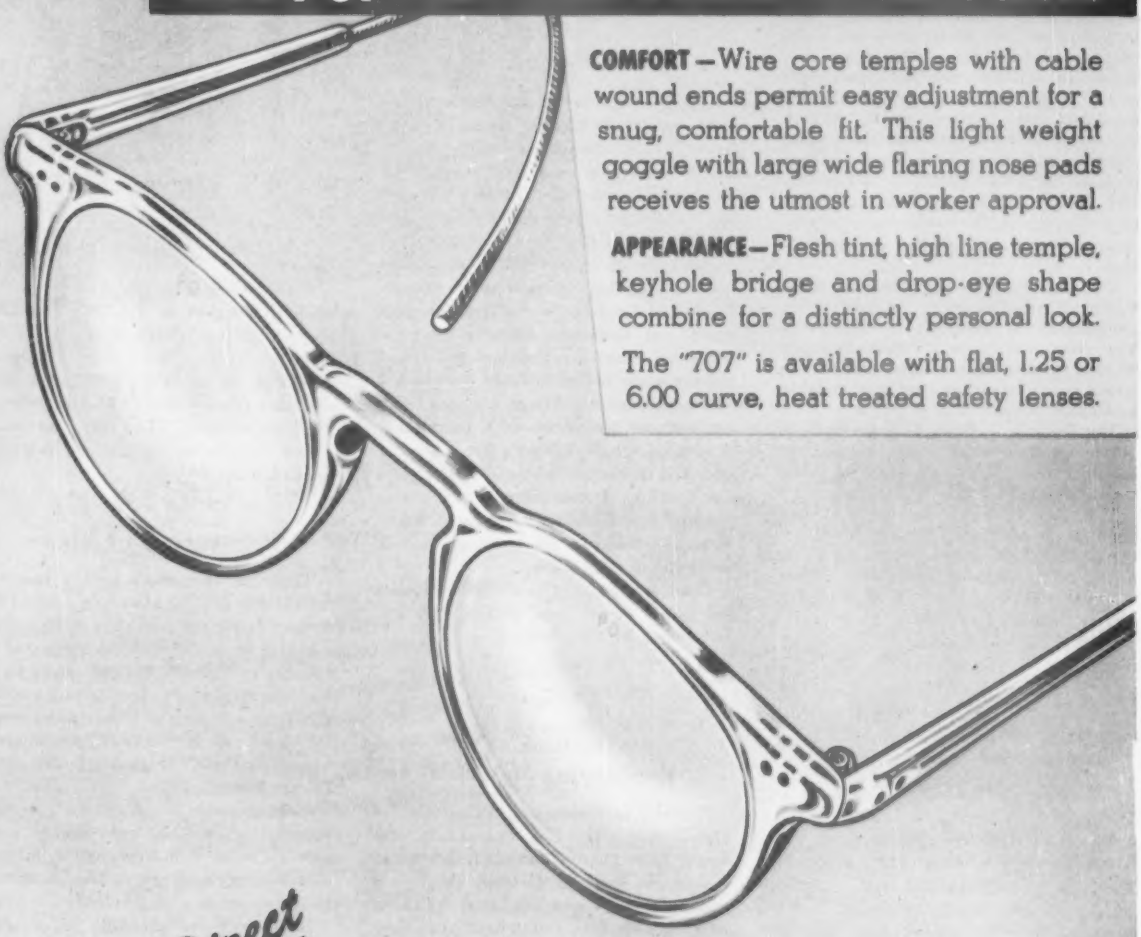


The new plastic "707" Safety Goggle combines

Protection — Comfort — Appearance

PROTECTION—is still the basic purpose of this new style goggle. Its acetate frame has been designed to retain the safety lens even when subjected to severe impact. In addition to its general use throughout industry where eye hazards are encountered, the non-sparking "707" is especially suitable for use in chemical and electrical plants.

MODEL 707 PLASTIC FRAME SPECTACLE TYPE SAFETY GOGGLE



COMFORT—Wire core temples with cable wound ends permit easy adjustment for a snug, comfortable fit. This light weight goggle with large wide flaring nose pads receives the utmost in worker approval.

APPEARANCE—Flesh tint, high line temple, keyhole bridge and drop-eye shape combine for a distinctly personal look.

The "707" is available with flat, 1.25 or 6.00 curve, heat treated safety lenses.

Order Direct and Save!

PENNSYLVANIA OPTICAL COMPANY

READING, PENNSYLVANIA

Known for Fine Ophthalmic Products Since 1886

NEW LITERATURE

Welding Stainless Steel

Welding Processes and Procedures Employed in Joining Stainless Steels, by Helmut Thielsch was published by the Welding Research Council as Bulletin No. 14 in September 1952. It consists of 48 pages.

It is a detailed *handbook-type* review of published, and the latest unpublished, information on metal-arc welding, carbon-arc welding, gas welding, resistance welding and flame cutting of stainless steels—including the recent developments and modifications of these processes. The welding characteristics, advantages and limitations of the various processes as well as the recommended procedures are discussed in detail. Moreover, for each of the major processes the current welding practices are reviewed with respect to preparation of base steel, joint design, jigs and fixtures, welding techniques, defects in welded joints, their causes and prevention, mechanical properties and applications.

For the various processes detailed tables are included, giving welding recommendations for electrode or filler metal wire sizes, current and voltage settings, welding speeds, joint designs, etc., for different sizes of stainless steel base metal to be welded or cut.

The Bulletin includes 242 references to published and unpublished information supplied by various authorities, 31 tables and 16 figures.

Individual copies may be purchased through the AMERICAN WELDING SOCIETY, 29 W. 39th St., New York 18, N. Y., at $2.00 each. Requests for bulk lots, 10 or more, should be sent to the Welding Research Council, at the same address.

Ampco Welding News

A special review issue constitutes the third quarter release of *Ampco Welding News*. This particular edition has increased from the usual four pages in one color to 12 pages and two colors. Generally, it reviews actual application stories of interest to most metal users.

Profusely illustrated—52 different actual welding applications using Ampco-Trode and Phos-Trode electrodes are pictured and described. Among the applications shown are overlaid gears and pinions made of cast iron or steel; steel mill maintenance welding; overlaid bearing surfaces and draw dies, fabrication of Ampco Grade 8 sheet and plate for wear and corrosion-resistant services; and the welding of dissimilar metals. Also featured and described are Ampco Safety Tools and Ampco Weld resistance welding electrodes and accessories.

Your free copy may be obtained by writing Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis.

Advanced Mechanics of Materials

The book *Advanced Mechanics of Materials*, by Fred B. Seely, M.S., Professor Emeritus of Theoretical and Applied Mechanics, and James O. Smith, A.M., Professor of Theoretical and Applied Mechanics, University of Illinois, is published by John Wiley & Sons, Inc., 440 Fourth Ave., New York 16, N. Y. The price is $8.50 per copy.

It was prepared primarily for advanced undergraduate and first-year graduate students in engineering, although in selecting the topics and methods of presentation the needs of design and research engineers were kept in mind.

The book consists of six parts: Part One, "Preliminary Considerations." Part Two, "Special Topics on the Strength and Stiffness of Members Subjected to Static Loads." Part Three, "Localized Stress—Stress Concentration." Part Four, "Energy Methods." Part Five, "Influence of Small Inelastic Strains on the Load-Carrying Capacity of Members." Part Six, "Introduction to Instability—Buckling Loads."

Textbook of Engineering Materials

Textbook of Engineering Materials, by Melvin Nord, Dr. Eng. Sci., LL.B., Associate Professor of Chemical Engineering, Wayne University, Detroit, Mich., is priced at $6.50 and published by John Wiley & Sons, Inc., 440 Fourth Ave., New York 16, N. Y.

The first half of the book is a general treatment of the subject. Beginning at an elementary level, it introduces fundamental concepts in physics, chemistry, geology and other basic subjects. Practical problems of importance are solved in each of the various branches of engineering. Here the scattered information about engineering materials is gathered and interpreted. The raw materials of engineering, the manufacture of engineering materials from the raw materials and the properties of engineering materials are all studied in a general way.

Then, in the second half of the book, the specific materials of engineering are treated in the light of the generalizations made. The basic engineering concepts introduced in the first half are applied to problems of occurrence, problems of production, problems of properties and the uses of various engineering materials. This treatment of the subject matter shows the student how to apply simple concepts in science to engineering, and how economic factors affect engineering practice. Thus, *Textbook of Engineering Materials* is an introduction to those primary engineering concepts which the student will find amplified in all further engineering courses.

Weldaluminite Brochure

A technical brochure of four pages copiously illustrated is available from the Spekaluminite Co., 100 South Water St., Ossining, N. Y. This brochure gives information on welding aluminum killed steel and rimmed steel with the different welding processes. The use of weldaluminite for eliminating porosity inert arc welding is stressed.

References on Fatigue

There has been issued by the American Society for Testing Materials a list of references to articles published in 1951 dealing with Fatigue. This was prepared by Committee E-9 on Fatigue (a group of leading experts on fatigue testing and the significance of fatigue properties) through the work of a Survey Subcommittee headed by Prof. T. J. Dolan, University of Illinois, Urbana.

Brief abstracts of the articles have been included when these were readily available. The list is not exhaustive but may be sufficiently extensive to be of interest to those concerned with this field.

References are generally so arranged that sheets can be readily cut apart for filing according to any desired plan. This is the second list of fatigue references in as many years, the first list (1950 references) having appeared in October 1951.

Copies of the list of References on Fatigue, comprising 22 duplicated pages, 8½ by 11 in., can be procured from ASTM Headquarters, 1916 Race St., Philadelphia 3, Pa., at $1.50 each, to both members and nonmembers.

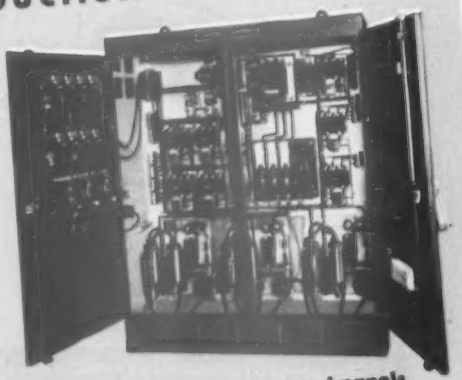
DESIGN LEADERSHIP

IN WELDER CONTROL FOR MASS PRODUCTION INDUSTRIES

A leading automobile manufacturer asked Square D to design magnetic and electronic control for a battery of 10 new multi-transformer welding presses. As many as 168 spot welds are made by one machine with 14 individually controlled timing periods in sequence.

Among the "must" requirements were:—welding current flow synchronized to prevent high starting transients and minimize stress on transformer windings; and a limitation of 1000 ampere current in each 3 phase line. *These 10 controllers are still providing trouble-free service after two years on the production line.*

Engineering counsel on any electrical control problem is available through Square D field offices in all principal cities.



One of the completed control panels ***Because Square D's line of control is so complete, this entire unit was designed using only standard devices, accessories and electronic sub-assemblies.***

SQUARE D COMPANY

More About Boron Steel

Developments in the use of boron as a powerful constituent of engineering and mechanical steels have changed the dark prospects for an adequate supply of alloys into something like a windfall for the makers and users of steel and for the future strength of America's industrial and military might.

In the short period of two years, the use of boron has increased fivefold, and as a result of this increased use, the interest in, and demand for records of experience with boron steel make it highly desirable that such records be made available.

Boron Steel is the title of an 80-page book, published by the American Society for Metals in 1951, the first printing of which is already exhausted. This same book, *Boron Steel*, is now being reissued with many added developments, according to an announcement just made by W. H. Eisenman, National Secretary of ASM.

"The timeliness of this volume on boron steel gives it a special place in the interests of steel users everywhere," Eisenman said. "The shortages of some of the most important alloying elements for steel are becoming less and less critical because the use of boron amplifies the effect of those alloying elements, and thus the available supply is made to answer for the greater demand. Very often the equivalent boron steel is also cheaper than the old-fashioned compositions. Thus patriotism and economy go hand in hand."

Eisenman pointed out that in an emergency, when alloy steels would have to be increased to the limit, the use of boron steel would in all likelihood increase to 40% of the total American output of alloy steels, in contrast to the present 10% and the 2% in 1950.

Weldability of Metals

A 141-page book, *Weldability of Metals* explains factors determining weldability; gives causes and cures for hard-to-weld metals; details welding procedures for steels, nickel, iron, alloys, copper, aluminum and hard facing. Reprinted from the 9th edition of the *Procedure Handbook of Arc Welding Design and Practice*. Publisher, The Lincoln Electric Co., Cleveland 17, Ohio. Price, 50 cents in USA; 75 cents elsewhere, postage paid.

The various types of carbon and alloy steels are described by giving nominal chemical analysis, properties, uses and characteristics. The best welding procedure for each is completely detailed. AMERICAN WELDING SOCIETY specification for electrodes used to weld are also given. The same treatment is given to copper, aluminum and nickel as well as their various alloys. Cast iron, forgings, cast steel, wrought iron, ingot iron, galvanized steel, terne plate and enameling stock are similarly covered. A thorough analysis of the principles and practices of hard surfacing is presented. Complete tabular data, drawings and pictures supplement the text.

Pocket Manual of Arc Welding

A handy, pocket-sized reference book for welding engineers, supervisors, inspectors and management personnel, *Pocket Manual of Arc Welding* contains 172 pages of valuable and interesting information on arc welding. It contains specialized data for those people who have felt a need for a quick and concise guide to the many phases of arc welding which they encounter each day.

The book has been written in a "down-to-earth" manner by Lew Gilbert, editor of *Industry and Welding* magazine, who has a rich background of practical and technical experience. Long identified with arc welding research and development, he has conducted exhaustive investigations—practical and technical—into the joining of metals by the electric arc.

Containing 22 chapters, this book features "how-to" information on welding of mild, alloy and stainless steels, as well as hard surfacing and welding of other fer-

WHY "SWEAT IT OUT"

FOR LACK OF CORRECT ADVICE?

A smart golfer goes to the pro when he wants to cure himself of the hooks and slices that ruin his score. Twenty minutes' instruction is all it takes. If you're having difficulties on any welding problem we would like you to think of us as your "pro." We're anxious to help...in every way we possibly can. That's our business.

Just phone your nearest CHAMPION sales agent or distributor . . . let him offer some professional advice on your welding procedures. Of course there is no cost or obligation.

THE CHAMPION RIVET CO.

CLEVELAND, OHIO • East Chicago, Ind.

EUTECTIC WELDING ALLOYS CORPORATION

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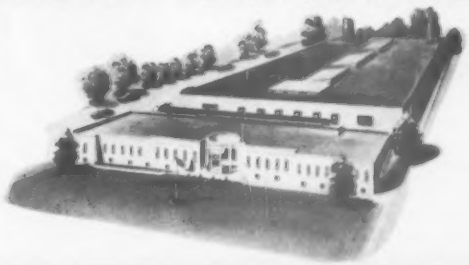


PRIZE COMPETITION FOR CONTRIBUTIONS TO THE SCIENCE AND ART OF "NON-FUSION" WELDING, BRAZING AND SOLDERING

Rules:

OPEN TO:
Engineers, Metallurgists, Research Investigators, Welders, Students, Faculty Members and all others qualified.

Covering technological and research aspects, procedures and applications of the use of lower melting (lower than parent) filler metals in the non-fusion welding processes.

The Jury and Committee of Awards:

1. Dr. Robert Humphrey, former Vice Chairman, American Society for Metals
2. Prof. Otto H. Henry, Dept. of Metallurgy, Polytechnic Institute of Brooklyn
3. Dr. Th. I. Leston, Vice President, Eutectic Welding Alloys Corporation

ELIGIBILITY

1. The Prize Contest is open to single individuals or groups of two or more, except employees of the Eutectic Welding Alloys Corporation, its subsidiaries and affiliates, its Advertising Agencies and its Distributors.

2. Each paper submitted must be original and previously unpublished. Moreover, each contestant must have actively participated in the work and preparation of the subject matter of the manuscript.

3. Participation necessitates submission of a paper of not less than 1,000 words, exclusive of drawings, specifications, tables, photographs, etc.

SUBJECT MATTER

1. The subject matter shall pertain to one of the following:

Classification A—*Basic principles, theory and results of research* in the field of "non-fusion" welding, brazing or soldering. (The metallurgical considerations of these processes may include Surface Alloying, diffusion, wetting, spreading, fluxing, equilibrium and non-equilibrium reactions, solidification, etc.).

Classification B—*Practical welding applications*—(The manuscript may be based upon the design or redesign of equipment, structures, and other products of ferrous or non-ferrous materials fabricated by "non-fusion" welding, brazing or soldering. Technical advantages of these processes and savings in material and labor should be considered and substantiated. Improvements in brazing and soldering techniques might also be the subject of the manuscript.)

2. Photographs, drawings, charts or test data which will add clarity to the presentation should be included.

SUBMISSION

1. Manuscripts should be typed in English, double spaced on 8½" x 11" white paper, with any drawing and photographs on separate sheets, preferably attached to the text. The manuscript should be securely bound and have a protecting cover.

2. Papers must be submitted in duplicate. One copy must be signed by the author (or authors) and enclosed in a separate sealed envelope, with the following information on the cover sheet (1st page) of the manuscript and on the outside of the envelope:

Name
Address
Signature
Company (or University) affiliation
Classification ("A" or "B") under which paper is submitted.

A duplicate of the paper, unsigned and unidentified as to author and application, should be enclosed in a second envelope.

The two envelopes should be placed in a large envelope and mailed to The Secretary, Prize Competition, Eutectic Welding Alloys Corporation, 172nd Street and Northern Boulevard, Flushing 58, New York, N. Y.

This procedure insures that all manuscripts are treated confidentially by the Jury. After the unidentified papers have been judged, the identification with the original paper on file with the secretary will be made and the prizes will be paid to the winners.

JUDGING

The papers will be judged by the Jury and Committee of Awards. In classification "A" the primary considerations are (1) originality, (2) understanding of basic principles, (3) merit of research results, (4) clarity of illustrations and data presented. In classification "B" the primary considerations are (1) originality, (2) material and labor savings, (3) improvement in efficiency and service life and (4) design and welding procedures.

Decision of the judges will be final. All entries and ideas become the property of The Eutectic Welding Alloys Corporation, and none will be returned.

CONTEST DEADLINE

All manuscripts have to be submitted and postmarked no later than August 31, 1953. Winners of awards will be notified approximately within (60) days after the close of the competition.

All communications and questions should be addressed to The Secretary, Prize Competition, Eutectic Welding Alloys Corporation, 172nd Street and Northern Boulevard, Flushing 58, New York.

Helpful suggestions on the preparation of the paper and on possible topics are available and will be sent upon request.

AWARDS

CLASSIFICATION A: Basic Principles, Theory and Results of Research		CLASSIFICATION B: Practical Welding Applications	
First Prize	$500	First Prize	$200
Second Prize	300	2 Second Prizes, each	100
Third Prize	200	3 Third Prizes, each	50
3 Fourth Prizes, each	100		
3 Fifth Prizes, each	50		

EUTECTIC WELDING ALLOYS CORPORATION
172nd Street and Northern Boulevard, Flushing, New York

Please send me ..... "Helpful suggestions on the preparation of papers for the EUTECTIC Prize Competition and Program of Awards."

Name .....

Affiliation .....

Address ..... Zone .....

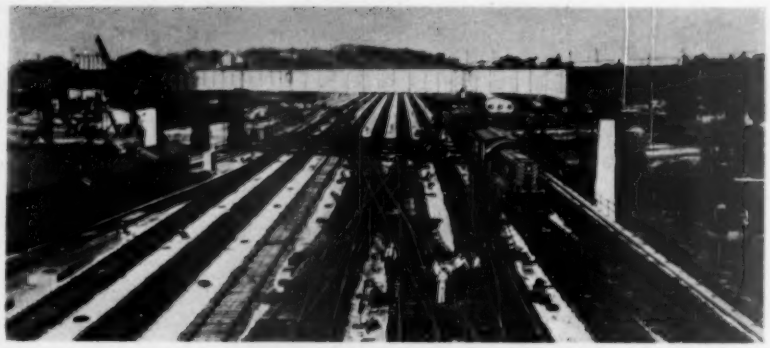
City ..... State .....

WJ-1

rous metals. "Causes and Cures of Common Welding Troubles," "Welding Symbols and How They Are Used," "Simple Qualification Tests for Operators" and "Types of Joints and Typical Welding Positions" are some other chapter titles.

Included in the amply illustrated book are a wealth of valuable comparative charts which list the various types of arc-welding electrodes by AWS classification and comparable electrodes made by various manufacturers.

The book sells for $1.25 and can be obtained by sending cash, check or money order to Industrial Book Co., 1240 Ontario St., Cleveland 13, Ohio. Discount rates are available for quantity purchases in excess of 25 copies.



Girders of this Kansas City highway overpass metallized with 0.012 zinc to protect the steel from corrosive effects of flue gases from locomotives passing beneath 24 hours per day

Corrosion Prevention with Metallized Zinc or Aluminum

Maintenance-free, life expectancies upward of 25 to 30 years on iron and steel structures and equipment is claimed in a new bulletin released by the Metallizing Engineering Co., Inc., Long Island City 1, N. Y. The bulletin describes and illustrates the application of 18 basic engineering specifications which provide long-term, economical protection against corrosion.

Industry each year pays a corrosion bill estimated at anywhere from 5 to 20 billion dollars. Many years have been spent searching for a practical approach to this problem. However, now it can be reported that tests in actual application over the past 25 years have produced sufficient evidence to demonstrate the positive protection provided by pure metallized zinc or aluminum. Life expectancies for protective coatings of this nature range upward of 25 to 30 years, usually without further maintenance.

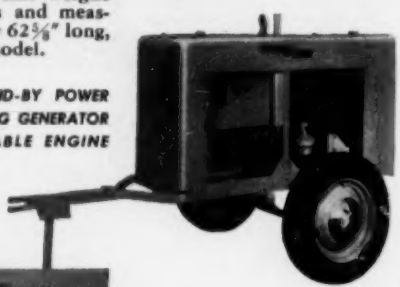
The bulletin illustrates and describes several typical applications, one of which is the application of metallized zinc on electric underground conduits. A paint job here usually lasts about six months to a year, when the paint begins to flake off. Two years after the conduits were metalized, inspection revealed no change in the coating. Also shown is a 15,000-gal sprinkler tank, the interior of which was sprayed with 0.006 in. pure zinc in 1934—no trace of corrosion in 1950. And yet, another case history of a coal barge in the Delaware River that was metallized in 1937 and 15 years later was still in excellent condition.

NEW ENGINEER-DRIVEN D-C WELDER with A-C power take-off

The new Westinghouse Engine-Driven Welder was developed for construction and field-maintenance operations. It provides 200 amperes of d-c current for welding. It also provides 3,000-watt, 110-volt, a-c current for stand-by power. By simply plugging into convenient receptacles on the a-c power panel, the operator may obtain power for drills, grinders, pumps, lights and other electrical equipment.

The standard unit is skid mounted for truck transport or may be mounted on a high-speed, pneumatic-tired trailer. Ready for field service, the unit weighs only 1,150 pounds and measures 39½" high by 62⅝" long, in the stationary model.

★ CONVENIENT STAND-BY POWER
★ IMPROVED WELDING GENERATOR
★ EFFICIENT, RELIABLE ENGINE

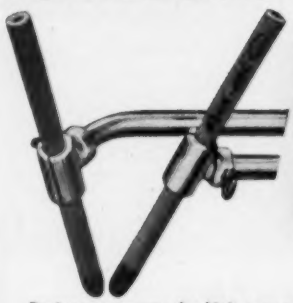


J-21713

You can be SURE... if it's Westinghouse WELDING EQUIPMENT

FOR MORE INFORMATION
Write to the Westinghouse Electric Corporation, Welding Division, Buffalo, New York, or contact your nearest Westinghouse representative.

BECKER
KEEN-ARC CARBONS



FOR CONSISTENTLY GOOD RESULTS

Designed to carry the high currents necessary for intense heat, BBB Keen-Arc Carbons produce a fine-grained weld of high tensile strength. They give a smooth, steady "flowing" flame which does not wander and which is concentrated at the desired focal point. Flame temperature is easily and accurately adjusted by merely changing the ampere input, and heavy copper coating permits gripping at extreme ends—eliminates frequent and periodic resetting.

A COMPLETE LINE OF CARBON WELDING SUPPLIES including carbon and graphite electrodes, carbon rods and plates, welding paste, etc. Write for catalog.

BECKER BROTHERS CARBON CO.
3450 South 52nd Ave. Cicero 50, Illinois

NOW!

LOOK TO REXWELD HARD SURFACING ROD

IT'S BETTER THESE WAYS:

CUT WELDING TIME

1. Rexweld builds up and holds on an edge better! This means less welding time is required.

INCREASE PRODUCTION

2. Rexweld flows more readily on flat surfaces! In machine gas welding operations valve companies have reported an increase in production per shift using Rexweld.

LESS POROSITY TENDENCY

3. Rexweld has less tendency for porosity because of its smoother flowing and better wetting properties.

BETTER WEARING QUALITY

4. Tests show Rexweld has better wearing qualities.

Hundreds of satisfied users, manufacturing such products as valves, oil pump parts, mixer shafts attest to Rexweld's ability to do a better job . . . at less cost! Rexweld is available in a wide range of grades to meet all requirements. Then too, Rexweld meets Government specifications. If you use hard surfacing rod, be sure you check Rexweld . . . it'll save you time and money.

CRUCIBLE REXWELD HARD SURFACING ROD

52 years of Fine steelmaking

CRUCIBLE STEEL COMPANY OF AMERICA, GENERAL SALES OFFICES, OLIVER BUILDING, PITTSBURGH, PA.

You may receive a copy of this useful bulletin by writing to Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y. Ask for *Bulletin 62B*.

Flexible Tubing Catalog Available

Flexible tubing for ventilation, fume and dust removal, and materials handling is described in a new 8-page bulletin available from Flexible Tubing Corp., Guilford, Conn. Catalog C2-4 gives detailed information on Spiratube A and Spiratube R, two types of highly flexible lightweight tubing made from continuous helical coils of spring wire wound with overlapping plies of specially treated multicoated fabric. Spiratube A is designed for ventilation, dust removal, fume disposal and similar applications—either blowing or exhausting.

Welding Equipment Catalog

The Modern Engineering Co. of St. Louis, manufacturers of "MECO" Welding Equipment, has announced the completion of their new catalog No. 150.

This catalog contains every type of welding equipment necessary for all oxyacetylene welding and cutting operations.

The text, plus over 150 illustrations, provide a complete and comprehensive guide for every welder. Each item of equipment is explained in detail with specific recommendations for its use.

Copies are available upon request. Write to the Modern Engineering Co., Dept. 93, 3415 W. Pine Blvd., St. Louis 3, Mo.

Booklet on Flame-Plating

A booklet describing the features of a new service—the application of undiluted coatings of powdered metals, such as tungsten carbide, to metal parts—has been announced. This new 12-page illustrated booklet, entitled "Flame-Plating," has been published by Linde Air Products Co., a Division of Union Carbide and Carbon Corp.

This booklet, Form 8065, can be obtained without charge or obligation from the nearest Linde office, or by writing direct to Linde Air Products Co., 30 E. 42nd St., New York 17, N. Y.

Employment Service Bulletin

Services Available

A-633. Welding Engineer desires position with a company in the East or Midwest. Experience includes six years as research assistant at University of Prague and Lehigh University, and six years as welding engineer and chief welding engineer at the Skoda Works in Pilsen and Prague. Degrees: Dipl. Eng. in Civil Engr. and M.S. in Metallurgy. Age 37, unmarried. Citizenship status: permanent resident with first papers.

Positions Vacant

Welding Engineer or Metallurgist with minimum of five years' experience, preferably in welding of pressure vessels and piping. Development and production follow work on components. Background in fabrication in stainless steel highly desirable but not essential. Address reply to Supervisor, Industrial Relations, Westinghouse Atomic Power Division, P. O. Box 1468, Pittsburgh 30, Pa.

V-283 (See advertisement below)

V-284. Position as Distribution Manager available for man with proved experience in nation-wide sales promotion and establishment of jobber's distributorships for arc and inert-arc welding equipment and accessories. Give complete résumé as to past experience, employers, compensation received and expected. All communications confidential.

REMOVES FUMES—DUST FASTER!
High Quality
PRESSURE BLOWER
only $49.50
REG. PRICE $70.00
For true fast, efficient elimination of welding fumes, sawdust, or other air impurities you can depend on this precision-made, all aluminum high pressure blower. Made with 5½" inlet and 3½" outlet. Enclosed ¼ H.P. Westinghouse single phase motor, 115 V., A. C., provides 3450 R.P.M. and up to 450 C.F.M. Immediate delivery! Ask for #BL50W.
STANDARD ELECTRIC
MFG. CO., INC.
WEST BERLIN 51, N.J.

WELDING ENGINEER

Welding Engineer for varied research program of major petroleum refiner, location Chicago. Graduate metallurgist or mechanical engineer, preferably M. S. with welding training, with experience in welding research. Reply with details of education and experience and salary requirements. Replies held confidential. Box V-283.

UNITED STATES TESTING COMPANY, INC.

INSPECTION and TESTING of Welding and Weldments

QUALIFICATION of Procedures and Operators

Main Laboratories **Hoboken, N. J.**

Boston - Chicago - New York - Philadelphia - Providence

Introducing ZIRTUNG

Exclusively Developed by Sylvania

Amazing New Tungsten Alloy that minimizes contamination and electrode consumption in inert gas arc welding.

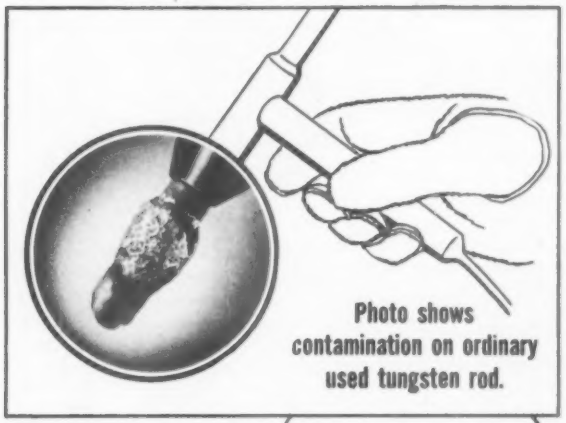


Photo shows contamination on ordinary used tungsten rod.

Photo shows negligible contamination on used ZIRTUNG rod.

"YES, SIR, ZIRTUNG FINALLY LICKED THE PROBLEM OF CONTAMINATION IN TOUCH STARTING!"

CHECK THESE ZIRTUNG ADVANTAGES

- Far less contamination from material being welded.
- Self cleans contamination without loss of electrode.
- Greatly reduces electrode consumption.
- Greater arc stability.

Remember the name ZIRTUNG! It's a vastly improved tungsten-zirconium alloy developed exclusively by Sylvania for inert gas electric arc welding.

Zirtung rods offer tremendous improvements in all major phases of inert gas arc welding. They save time, save electrodes. Operate in the same current ranges as thoriated tungsten, and they contain no radioactive material.

You'll find Zirtung works wonders on problem metals like magnesium, mild steels and aluminum. Order Zirtung rods today. Sold only by your friendly Sylvania Welding distributor who also handles Sylvania pure tungsten and thoriated tungsten electrodes. Available in all common sizes and finishes. For further information, address: Sylvania Electric Products Inc., Dept. 3T-4901, 1740 Broadway, New York 19, N. Y.

SYLVANIA

TUNGSTEN AND CHEMICAL PRODUCTS; RADIO TUBES; TELEVISION PICTURE TUBES; ELECTRONIC PRODUCTS; ELECTRONIC TEST EQUIPMENT; FLUORESCENT TUBES, FIXTURES, LIGHT BULBS; PHOTOLAMPS; TELEVISION SETS

NEW PRODUCTS

Arc Oxygen Cutting Holder

Now available for arc oxygen cutting is the patented Weldwire Cutting Electrode Holder. Sturdy enough for all the tough jobs, the holder and the Weldbest arc oxygen cutting electrode are used for cutting and piercing all metals, especially those difficult to cut by ordinary means.

The holder is low priced and constructed of standard parts. Maintenance is very easy.

Holder and cutting electrodes are manufactured by Weldwire Co., Inc., Philadelphia 25, Pa.

Tweco Announces New Ground Clamp

To round out its complete line of arc-welding ground clamps, Tweco Products Co., Box 666, Wichita, Kan., has just announced the new, low-priced, Model GC-200 "Cub" Ground Clamp which has a rated capacity of 200 amp.

Job shops, farm welders and users of inert-arc welding will welcome this new tool which is designed to meet their requirements. Following the basic design that has gained such popularity for other Tweco ground clamps, the "Cub" features a protruding upper lip for extra leverage in applying the clamp, wide jaws for added conductivity, serrated lower jaw which removes rust and scale as the clamp is applied and assures a clean connection, a strong and fully insulated spring, and simple bolt and clamp connection.

Modern welding equipment and techniques demand the utmost efficiency in the welding circuit. This is achieved by sound connections and positive grounding. The Tweco "Cub" makes it possible to shift ground connection easily and quickly to reduce "arc blow," and shorten the welding circuit for current economies.

Tweezer Spot Welder

Unitek Corp., 275 N. Halstead Ave., Pasadena, Calif., announces the development and production of the Model 1012 Unimatic Tweezer-Type Spotwelder. The new precision Unimatic Model 1012 features the proved advantages of the Unimatic power principle combined with a high performance "tweezer-type" electrode switch assembly. The unit has been designed to meet requirements for a spot welder capable of producing instrument

quality welds with production line efficiency.

This new model incorporates a unique electrical circuit . . . insuring welds that are consistently uniform, burn-free, spatter-free and dependably secure. The electrode-switch assembly provides rapid and accurate automatic control of welding pressure. Welding versatility extends over a wide range of materials and thicknesses. Equally satisfactory results are obtained whether the requirement calls for welding two 0.0008-in. diam copper or nichrome wires . . . or two 0.018-in. sheets of Type 302 stainless steel.

The small hand-fitting electrode-switch assembly is simple to operate, and well designed for making accurate welds rapidly in confined and difficult locations. It is a compactly packaged unit, completely portable, and provided with extra long power leads. For applicational data, literature and further details write Unitek Corp., Pasadena 8, Calif.

Cover Goggles

New, larger Cover Goggles to fit over modern glasses, have been announced by the Chicago Eye Shield Co. These new goggles, known as series 548 and 549, are actually 20% larger than previous models. They are available for both chippers and welders.

Wider bearing surfaces in the molded plastic frames eliminate pressure points and provide a perfect seal which keeps out flying particles or light flashes.

Another new feature in the chippers goggles is indirect ventilation through the slotted lense rings. Direct ventilation in this type is provided through perforated side shields. Welders and dust models are indirectly ventilated through side shields. Both models have considerably larger air space within the goggles to reduce fogging.

Lenses are quickly replaceable from the front. Standard 50-mm lenses seat flush with rings to deflect flying objects. Three adjustable headbands are offered: Comfort King round rubber without slides or buckles, elastic or flat rubber.

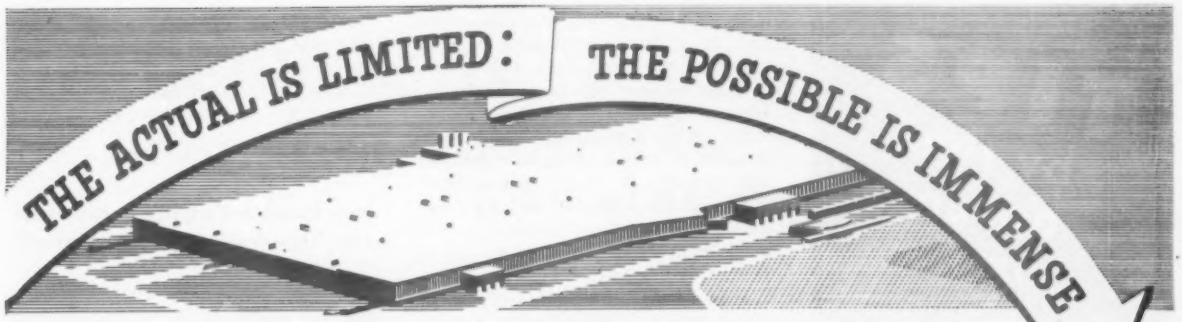
Further information can be obtained from CESCO safety equipment distributors or by writing the Chicago Eye Shield Co., 2300 Warren Blvd., Chicago, Ill.

Linc-Thaw

The Linc-Thaw, a new protective device manufactured by the Lincoln Electric Co. of Cleveland, Ohio, will give an engine-driven welding machine protection from overload damage when the machine is being used to thaw frozen water pipes.

No welder should be used for continuous operation above its rated capacity. The machine may overheat, causing either serious damage to the insulation or may completely burn it out. The Linc-Thaw hooks into the output circuit of the welder and indicates at all times what current is passing through the lines. A fuse also limits current output to the rated maximum for the generator on continuous duty.

The unit can be attached directly to the frame of the machine or placed in any convenient location. The output leads of the



NEW LINCOLN PLANT CREATED BY INCENTIVE-INSPIRED CO-ACTION IN DEVELOPING POSSIBILITIES IN PRODUCT

© LE Co. 1952

WELDED STEEL DESIGN CUTS MANUFACTURING COSTS

. . . saves machining, simplifies production

MANY types and sizes of machine components can be produced more efficiently . . . with less material . . . at less cost through welded steel design. From heavy equipment like large presses (Fig. 1 & 2) to small machines (Fig. 3 & 4), proper design in welded steel saves materials, saves labor . . . cuts cost.

Material costs are less with welded steel designs because fewer pounds of metal are required on which less machining and less finishing is necessary. Products have better selling appeal with welded steel's modern appearance.

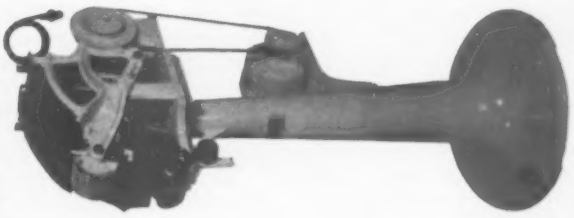


Fig. 4. Original Construction *of marking machine. Had high rejection rate in castings. Required considerable machining and finishing. Weighed 83 pounds.*

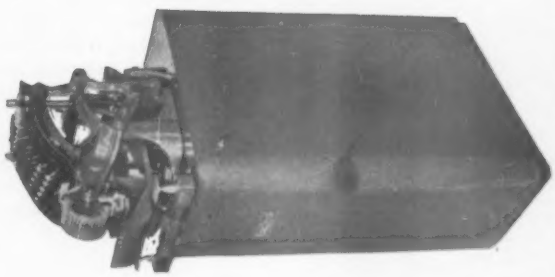


Fig. 3. Welded Design. *Cuts manufacturing cost 25%, weighs 33% less. Cabinet type base is now fabricated from 16 gauge metal and welded in simple fixture. Steel design eliminates entire cost of machining and handling originally required.* Courtesy National Marking Machine Company, Cincinnati, Ohio.

PROPER DESIGN IN WELDED STEEL IMPROVES PRODUCT AND LOWERS COST

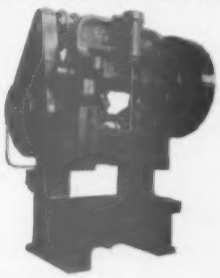


Fig. 1. Original Construction *of multiple punch press. Material cost was $.10 per pound from this manufacturer's own foundry. Weight was 56,000 pounds.*

INCREASES RIGIDITY SAVES MATERIAL

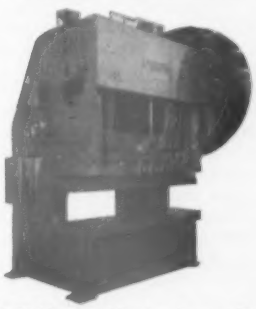


Fig. 2. Welded Steel Design *is actually more rigid than former casting yet weighs only 49,550 pounds. Modern in appearance . . . costs 15% less.* Courtesy Williams-White and Co., Moline, Illinois.

MORE PROOF

Machine Design Sheets available on request. Designers and Engineers write on your letterhead to Dept. 91.

THE LINCOLN ELECTRIC COMPANY

Cleveland 17, Ohio

THE WORLD'S LARGEST MANUFACTURER OF ARC WELDING EQUIPMENT

SPOT WELDING TIPS

Resistance Welding knows this symbol. In shops small and large, it means *dependability* in Tips for sound, clean welds—efficient cooling—speedy welding—long tip life—less down time—welding at a saving!

WW Tips are made of alloys of correct physical and electrical properties, precision-machined, water-tight and electrically tight fitting.

Replaceable tips, both straight and offset, WW leak-proof holders, seam welding wheels, electrodes and dies and special alloys for making your own dies are available in numerous standard sizes which you will find in the WW Catalog entitled "Standard Replaceable Welding Tips—Standard Water-Cooled Holders." Weiger Weed & Company, Division of Fansteel Metallurgical Corporation, 11644 Cloverdale Avenue, Detroit 4, Michigan.

Send for this free booklet of latest information on Resistance Welding.

machine are connected to one side and the leads to the frozen water service are attached to the other side. The welding machine is started and the output current adjusted according to the meter reading shown on the Linc-Thaw. The device is designed in several ratings for operation with Lincoln Shield-Arc engine-driven welders of 200-, 300-, and 400-amp capacity. It may be used, however, on any make or model of engine-driven welder. It must only be used with the proper lag-type fuse and must be used only with the size machine for which it has been rated.

Portable X-ray

Developed and widely used in Europe, the Fedrex portable X-ray unit for industrial radiography is now available in this country in a new and improved model under the trade name of Andrex. The unit represents a radical departure from traditional design, in that X-ray tube and other high-tension parts are combined in one single all-welded steel tank. High-tension cables, cooling connections, separate transformers and cooling pumps have all been eliminated, resulting in a unit so small and light that it can be carried by one man into working spaces that formerly were inaccessible or entered with the greatest difficulty. The compact simplicity of design also results in greatly reduced maintenance and repair costs. Andrex claims a remarkably high efficiency in operation. Field reports show that in erection work Andrex gives 2 to 3 times the number of radiographs per working day than any other equipment. Units are already being delivered in increasing demand to the Navy, and the aircraft, shipbuilding and petroleum industries. At present Andrex units are available in capacities of 120 and 175 kVp, but the manufacturer announces early production of a 250-kVp unit, and also various special models. A complete Andrex service and parts division is maintained in this country at all times. Holger Andreasen, Inc., 703 Market St., San Francisco, Calif.

New Powder-Cutting Equipment for Stainless and Alloy Steels

A new "Ferrojet" powder dispensing unit for powder-cutting of stainless and other hard-to-cut steels is announced by National Cylinder Gas Co., Chicago.

The new model provides greater uniformity of powder flow as a result of improvements in the powder regulating and ejecting mechanisms. It has been completely redesigned, is simpler and more compact than the former model, and costs substantially less. It is also easily portable: the dispenser and a cylinder of the powder-conveying gas can be mounted on an ordinary two-wheel gas cylinder truck.

Gas pressure and rate of powder flow are maintained automatically by the dispensing unit. The powder control valve is also automatic, operating simultaneously with the turning on and shutting off of the cutting oxygen valve on the torch.

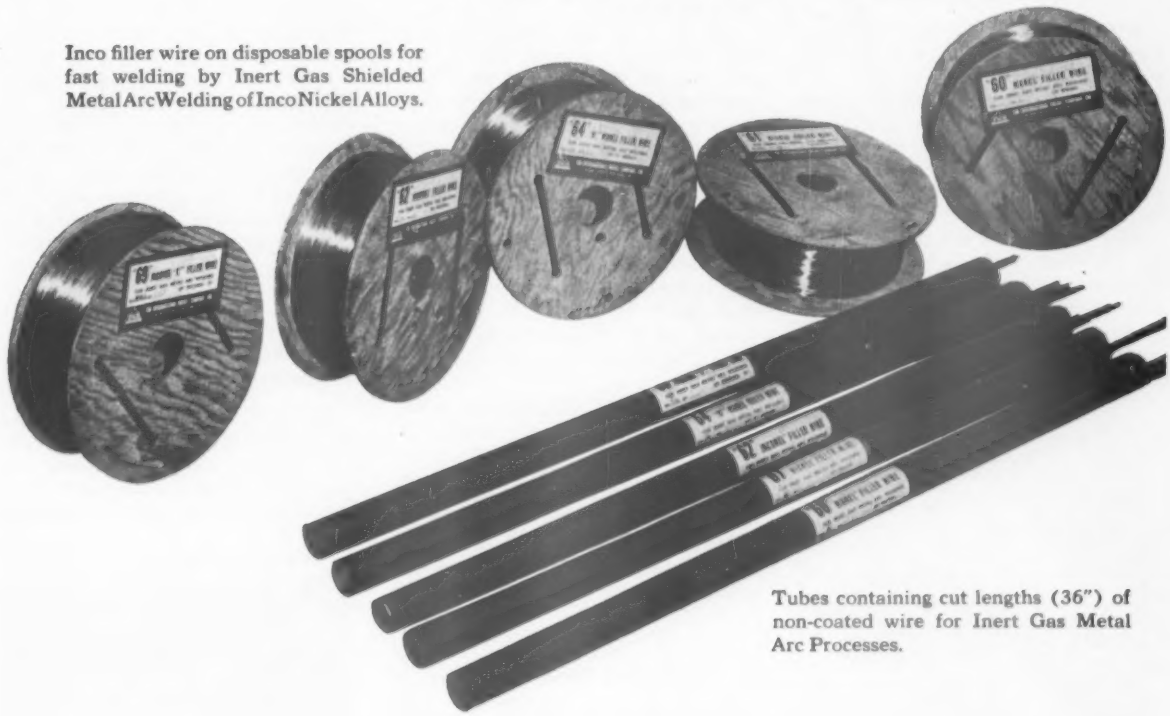
Both machine and hand cutting torches are available for use with the dispenser.

For use in foundries a Ferrojet flame washing torch has been developed for the quick removal of riser pads, chill bars, fins, etc., from stainless and other alloy steel castings. The torch is said to be up to ten times faster than chipping and grinding.

Introducing...

A COMPLETE LINE OF INCO FILLER WIRES ...FOR INERT GAS METAL ARC WELDING

Inco filler wire on disposable spools for fast welding by Inert Gas Shielded Metal Arc Welding of Inco Nickel Alloys.



Tubes containing cut lengths (36") of non-coated wire for Inert Gas Metal Arc Processes.

There's something new in filler wire for inert gas metal arc welding now.

Inco engineers have developed a special composition wire that will produce sound welds of the proper metallurgical balance on Inco Nickel Alloys.

Disposable spools of this new filler wire are available for any of the consumable electrode processes used to weld materials ⅛" thick and over. (For tungsten electrode process Inco produces rods in cut lengths of 36", in sizes from 1/16" to 5/32".)

The spools are supplied in wire diameters of .035, .045, and .062 in the Inco "60" series of welding materials.

For inert gas metal arc welding of Monel® you should specify "60" Monel Rod or Filler wire, for Nickel specify "61" Nickel Rod or Filler wire, for Inconel® – "62", for "K"® Monel – "64", for Inconel "X"® – "69".

Of course, like other Inco Nickel Alloy materials, filler wire is on extended delivery now. That's because so much is needed for defense. Therefore, it will pay you to keep in touch with your distributor regarding available supplies.

To help you determine which Inco welding materials to use for specific applications, Inco's Technical Service Section has prepared a Welding Materials Guide. It covers welding solid materials, clad materials, dissimilar metals and overlays. You can get a copy by writing directly to Inco.

INCO TRADE MARK

THE INTERNATIONAL NICKEL COMPANY, INC.

67 Wall Street, New York 5, N. Y.

PERSONNEL

J. F. Wagner of Burdett Elected by Oxygen Manufacturers

J. F. "Joe" Wagner, Vice-President-in-Charge of Manufacturing at the Burdett Oxygen Co., was elected Secretary of the Independent Oxygen Manufacturers Assocn.

The election took place at the Association's Annual Fall Business Meeting at the Edgewater Beach Hotel in Chicago, Ill., on Nov. 10–12th.

Mr. Wagner, who has been active in the Cleveland Section of the AMERICAN WELDING SOCIETY and Independent Oxygen Manufacturers Assocn., was also recently reelected Secretary-Treasurer of the Cleveland Section of the AWS for 1952–53.

J. S. Roscoe Appointed Executive Vice-President

J. S. Roscoe has been appointed Executive Vice-President in Charge of Business Administration by the Board of Directors of The Lincoln Electric Co., Cleveland, Ohio. William Irrgang, who was appointed Executive Vice-President in October 1951, is in charge of all manufacturing.

Roscoe has been with Lincoln since 1924 when he was graduated from the Ohio State University with a degree in Electrical Engineering. He has been Director of Purchasing since May 1951. Prior to that he was in the company's sales engineering department in various sections of the country. He has been District Manager for Lincoln in Syracuse, N. Y.; Pittsburgh, Pa., and Chicago, Ill.

During the 28 years Roscoe has been active in the arc welding field, he has pioneered the application of welding in many industries. One of his early developments was the application of automatic carbon arc welding of copper cooling fins to cylinders for air-cooled automobile engines. He also established the procedures for welding the first all-welded freighter on the Great Lakes.

As District Manager in Pittsburgh, from 1944 to 1947, he helped develop the vital maintenance of steel mill equipment through the use of automatic hidden arc welding to rebuild rolls and other mill parts.

He has been an active member of the AMERICAN WELDING SOCIETY, organizing the Syracuse Chapter and serving as an officer and Director of Chapters in Pittsburgh and Chicago. He is a member of the Western Society of Engineers and the Electrical Engineering Honorary Society, Eta Kappa Nu. He is married and has two children.

Boring Promoted

Tweco Products Co., Wichita, Kan., has announced the appointment of Donn Boring to the post of Advertising and Sales Promotion Manager. Prior to his association with Tweco, Mr. Boring held the position of Advertising and Sales Promotion Manager, and Ontario District Manager with Air Reduction Canada Ltd. Born in Canada, he came to the United States at an early age where he gained all of his education. He returned to Canada, entering the advertising business with which he has been associated every since. His connection with welding extends over the past 12 years during which time he edited the *Welders' Digest* in Canada. In his new position he will be responsible for all advertising and sales promotion activities.

Link Welder Corp. Names Harry Day Vice-President

Edward G. Biederman, President, and Glenn Cashdollar, Executive Vice-President, Link Welder Corp., jointly announced the recent appointment of Harry Day to Vice-President and General Manager.

Gaining broad experience in Detroit's metals working, fabricating and resistance-welding equipment industries, Mr. Day has functioned in the past as General Foreman of Wayne Die and Tool, Detroit, Chief Engineer for Multi-Hydromatic Welding and Manufacturing Co., East Detroit. On joining Link Welder Corp. as Chief Engineer, he subsequently became Plant Manager, then assumed the position of General Manager prior to his present appointment as Vice-President.

J. S. Roscoe

Donn Boring

Harry Day

After completing his academic education in Detroit's public schools, Day then attended the Lawrence Institute of Technology, graduating with a BME degree.

In projecting his operational policies, Day voiced the intention of expanding Link Welder Corp.'s representation and enfranchisement in numerous of the nation's industrialized sections. He stated that his firm's broadened line of unit resistance welders, portable gun units, and a wide variety of presses, fixtures, weldments and mutiple equipment would be presented comprehensively and aggressively to all fields of the metal fabricating trade. Mr. Day is a member of the AMERICAN WELDING SOCIETY.

The executive offices of the Link Welder Corp. are located at 13684 W. Buena Vista, Detroit 27, Mich.

Lamont Made Sales Manager

F. H. Lewis, General Sales Manager of the Industrial and Medical Gas Divisions of the Liquid Carbonic Corp., recently announced the appointment of William D. Lamont as Sales Manager of the Gasweld Division. Prior to joining Liquid, Mr. Lamont was President of the Detroit Industrial Welding Co.

William D. Lamont

Mr. Lamont has been active in all phases of heavy industrial welding since 1931. Before joining Detroit Industrial Welding Co., he had been associated with the Bird Gas Corp. and the National Cylinder Gas Co., both of Detroit.

Mr. Lamont also has wide experience in the tool-making field and has served as a consultant in the design of jigs and fixtures for welding applications in industrial plants.

an ACCO product for Stainless Steel WELDING

AC-DC Electrodes

GAS Welding Rods

made by PAGE

BURNING...
stable even at lower heats

SLAG...
clean, easily removed

COATING...
resists cracking down to very short stubs

SELECTION...
complete line for welding every type of stainless

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AMERICAN CHAIN & CABLE

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PAGE Welding Electrodes AND Rods

SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Annual Organization Meeting

Bethlehem, Pa.—The *Lehigh Valley* Section held its Annual Organization Meeting on June 4th at the Bethlehem Steel Club, Hellertown, Pa.

The officers and directors of the 1952–53 season elected at the annual meeting are: *Chairman*—R. D. Stout; *Vice-Chairman*—A. T. Barvaria; *Secretary-Treasurer*—R. E. Somers; *Director* (3-year term)—R. I. Fluck; *Director* (3-year term)—L. J. McGeady.

Dr. Stout appointed the following committee chairmen: *Auditors*—C. Schleiker; *Dinner, Hotel Arrangements*—L. J. McGeady; *Membership*—W. Price; *Coffee Speaker*—R. Cook; and *Publicity*—V. Edwardes.

Growth of Welding

Bethlehem, Pa.—Approximately 40 members and guests attended the dinner which preceded the monthly meeting of the *Lehigh Valley* Section held on October 6th at the Hotel Bethlehem. Mr. Fox of the Wire Rope Sales, Bethlehem Steel Co., gave an introductory talk in connection with the showing of a colored motion picture "Ropes of Steel," produced by the Bethlehem Steel Co. The picture showed the manufacture and ultimate use of various types of wire rope.

Fifty-three members and guests were present at the technical meeting to hear T. B. Jefferson [AWS], editor of the *Welding Engineer*, give a very interesting talk on "What Welding Means to America." Mr. Jefferson pointed out the expanding use of welding by stating that in 1940, welding equipment, assessories, etc., was a $97 million business and in 1951, was a $532 million business. A survey of the metal working industries showed 85% used welding. These industries ranged from iron ore mining to costume jewelry. Mr. Jefferson continued by discussing several things to ensure the continued growth of welding and concluded by pointing out how welding plays an important role in our every day lives and in home equipment that is considered commonplace.

Design Requirements

Bethlehem, Pa.—Forty members and guests attended a dinner at the Hotel Bethlehem held in connection with the regular monthly meeting of the *Lehigh Valley* Section on November 3rd. J. J. Kurth, former All-American tackle and a member of the 1930 Notre Dame Championship Football Team, was the coffee speaker. Kr. Kurth spoke of the attributes of Knute Rockne and his exceptional ability to know each player and instill in them the extra spark required to win.

Approximately 65 members and guests were present at the technical meeting to hear W. B. Hoyt, Materials Engineer of the M. W. Kellogg Co., speak on "Design Requirements for Pressure Equipment vs. Welding Quality." Mr. Hoyt's talk covered joint efficiencies for storage drums, pressure vessels and pressure piping work, the description basis of selecting these efficiencies, and their merits and failings. He stated that design and welding codes cannot cover all weld joint design and are an average of all designs, consequently the codes may penalize some designs. The problem, as he pointed out, is one of obtaining reliable welded joints and, by more rigid inspection, being assured the joints are sound. If this can be accomplished, the code committees would be in a better position to restudy and possibly increase the joint efficiencies or eliminate them entirely and resort to a factor of safety similar to the present design practice as applied to parent metals.

Mr. Hoyt showed slides and a question period followed his talk.

Plant Visits

Boston, Mass.—As a feature of the October meeting of the *Boston* Section held on the 2nd, plant visits were made to two adjoining plants, namely, the Artisan Metal Products Corp. and Metal Fabricators, Inc. Past-Chairman, James Donovan is Manager of the Artisan Metal Products plant and Harry Ferguson, Chairman of the Meeting and Papers Committee for several years, is Manager of the Metal Fabricators plant. These two well-equipped shops manufacture some very interesting precision-welded products. Socially and educationally the visits were a great success.

Two-hundred and sixty-five turned out for the plant visits which started at 4:30 in the afternoon and wound up at dinner with an attendance of 223 in one of the Artisan Metal Products Corp. buildings.

Electrode Problems

Boston, Mass.—H. F. Reid [AWS], Head of the Technical Service Welding Electrode Division, The McKay Co., York, Pa., was the speaker at the November 10th dinner meeting of the *Boston* Section held in the Westinghouse Auditorium. Mr. Reid's illustrated subject was on "Stainless and Mild Steel—Welding and Electrode Problems." One hundred and ten were present for the technical meeting.

One hundred were present at dinner in the Westinghouse Apparatus Warehouse. Coffee speaker was Frank Woolner, editor, *Salt Water Sportsmen*. Mr. Woolner showed color and sound movies on "Striped Bass Fishing."

Powder Cutting

Bridgeport, Conn.—James R. Craig, Linde Air Products Co., was the speaker at the technical meeting of the *Bridgeport* Section held on November 20th. Mr. Craig spoke extemporaneously on "Powder Cutting and Washing of Stainless Steel." Slides and motion pictures were used to illustrate the talk.

About 30 members and guests attended the dinner and meeting held at Rapps Restaurant in Shelton, Conn. Coffee speaker was Johnny "Red" Allen who told of some interesting boxing anecdotes.

Resistance Welding Equipment

Buffalo, N. Y.—F. A. "Dutch" Bodenheim, [AWS] of the Federal Machine and Welder Co., ably described some of the latest developments in resistance welding equipment to 70 members and guests of the *Niagara Frontier Section* at the October 30th meeting held in the Sheraton Hotel, Buffalo.

Mr. Bodenheim, reviewed a number of case histories in which "automation" (the replacement of standard methods and machines by newer, more fully automatic equipment) has resulted in reduced costs and higher production. This may be accomplished in several ways.

One method is that of combining operations performed by more than one welding machine into a single piece of equipment. A typical example was a part for an automatic clothes dryer, which was fc merly fabricated by separate spot- and seam-welding machines. These operations have been combined into a single machine, resulting in reduced labor and handling costs, and more uniform quality.

Another method of reducing handling and labor costs is that of incorporating the welding equipment into the process

Niagara Frontier Meeting. Left to right: F. A. Bodenheim, Jr., speaker; R. P. Gehring, Section Chairman, and J. E. Ponkow, Meeting Chairman

line. One example cited was the fabrication of automobile door panels, in which the parts are conveyed directly to the welder from the press, and after welding they are moved by conveyor to the next operation.

Costs may also be reduced by incorporating operations other than welding into the welding machine, thus eliminating excess handling and alignment. Several examples were shown, such as the fabrication of cylindrical barrels, in which a flat sheet is fed into the machine, formed into a cylinder, and seam welded, thus eliminating separate forming and spot-welding operations. In another case, a spot welder was combined with a tangent bending machine and a metal cabinet was formed and welded at one location.

In discussing power requirements for resistance welding, Mr. Bodenheim emphasized that the power problems at various plants need to be studied and solved individually, and that no single, cure-all method of solving these problems exists.

A lively, 45-min discussion period followed Mr. Bodenheim's presentation.

Prior to the technical session, the 64 dinner guests enjoyed a coffee talk by Bob O'Reagan of the Buffalo Hockey Club. In the afternoon, 55 Section members visited the new plant of the Stamping Division of the Ford Motor Co. for a very interesting 2-hr tour.

Symposium on Cutting

Cleveland, Ohio—Approximately 175 members and guests of the *Cleveland* Section attended the technical meeting which was a symposium on cutting and gouging of stainless steel and other nonferrous materials, held on November 12th. Approximately 140 persons had dinner at the Allerton Hotel before the melting.

The membership chairman, John Rogos, made capital out of the excellent turn-out and by urging all members to help put the section on top by bringing in 100 new members. He has adopted as a slogan for his campaign "Each one bring one."

The first Vice-Chairman and Program Chairman Lew Gilbert acted as moderator for a panel of seven experts representing seven different companies who produce cutting equipment which can be used on stainless steel and other alloys. Each of the seven was given 15 min to explain his process and equipment after which the meeting was opened to questions and discussion. The meeting provided an excellent opportunity to see and hear a broad survey of processes and equipment available for cutting stainless steel. Talks were illustrated with slides and movies. Samples of cutting and equipment were displayed.

A. F. Chouinard, AWS, with National Cylinder Gas, was the first speaker. He examined the difficulty normally encountered in cutting stainless and other alloys with a gas flame, pointing out that the formation of chrome and nickel oxides, which have a higher melting point than the base metal, form a heat shield over the surface of the metal to be cut. He described the equipment developed by his company for injecting iron powder into the cutting flame to overcome this difficulty. The equipment can be used with standard manual and mechanized torch-cutting outfits.

He was followed by R. L. Diely AWS, of Air Reduction Sales Co., who described Airco's equipment for using metallic and nonmetallic powder with oxy-acetylene cutting.

Myron D. Stepath AWS next described the torch he has developed for the Arcair Co. which is used in cutting and gouging. The process used with the torch combines the carbon arc with compressed air. It is a melting process rather than an oxidizing process. The high heat of the arc melts the metals and the compressed air blows the melted metal out of the cut or groove.

James E. Norcross AWS, described the Oxyarc method of cutting which the Arcos Corp. has developed. This process uses a hollow metallic electrode through which is forced a stream of oxygen. Oxidation occurs in the high heat of the metallic arc.

L. D. Richardson AWS described for Eutectic Welding Alloys Corp. the special cutting and gouging electrodes his company has developed. These are mild steel core wire electrodes used with standard welding equipment which have coatings developed to accelerate cutting action.

Albert Adams described a torch developed by the Kwik-Arc Co. which uses a metallic electrode and compressed air to melt the metal and blow it out of the cut or groove.

The last speaker was John Twohey who described the injection equipment of the Linde Air Products Co. for powder cutting with standard torch-cutting setups.

Position Welding

Dallas, Tex.—Anthony K. Pandjiris AWS, of the Pandjiris Weldment Co. gave an excellent semitechnical talk on "Set the Pace—Win the Race" at the October 29th meeting of the *Dallas* Section held in the auditorium of the Dallas Power and Light Co. Mr. Pandjiris' talk was accompanied by slides.

Pressure Vessel Design

Dallas, Tex.—Dr. H. C. Boardman AWS, Director of Research, Chicago Bridge and Iron Co., Chicago, gave an excellent illustrated presentation on the subject "Pressure Vessels and Design—Now and in the Future" at the November 26th meeting of the *Dallas* Section held in the Lone Star Gas Co. Auditorium.

The February 13th meeting will be addressed by Van Rensselaer P. Saxe AWS, Consulting Engineer, who will talk on the subject "Structural Steel Welding."

Resistance Welding Developments

Denver, Colo.—The *Colorado* Section held its regular monthly meeting in the Festival Room of the Oxford Hotel on Tuesday, November 11th.

Guest speaker was T. Embury Jones AWS, President of Precision Welder and Machine Co., Cincinnati, Ohio. Mr. Jones gave a very interesting and informative talk on "Developments in Resistance Welding" which, with slides, covered a large variety of machines for use in this process.

Coffee talk was given by Denver Police Chief H. E. Forsyth, who spoke of the problems and their solutions in the Police Department.

Low-Hydrogen Electrodes

Des Moines, Iowa.—The regular monthly dinner meeting of the *Iowa* Section was held at Younkers Dining Room on November 20th, with 46 members and guests present. James C. Norcross AWS, Sales Manager of the Arcos

Corp., was the guest speaker of the evening and presented an excellent address on "Low-Hydrogen Electrodes—Their Development and Uses."

In his presentation, Mr. Norcross defined low-hydrogen electrodes as electrodes with a coating low in H_2O content. All metals have a great affinity for hydrogen in the free state. During welding this hydrogen can collect in the fusion zones and the ensuing pressure cause underbead cracking. The use of low-hydrogen coatings eliminates the cause of underbead cracking. Another advantage of low-hydrogen electrodes is that low-alloy or high-carbon steel, sulfur or selenium bearing free machining steels, and steel which must be enameled after welding may be successfully welded without preheating. Mr. Norcross went on to discuss the research necessary to bring about this relatively new product and the part his company had in this pioneering period. He emphasized the limitations of the low-hydrogen electrode such as their susceptibility of moisture pick-up, the necessity of training welders in the proper manipulation of the arc, proper current settings etc. A few comments were made on storing, packaging and drying low-hydrogen electrodes.

The talk was concluded with the presentation of a number of slides graphically illustrating moisture control and reduction based on experimental data. A short discussion period followed.

The meeting was presided over by the Vice-Chairman R. Rogers, who also announced the plans completed for the ensuing Christmas party.

Newly Elected Officers

Des Moines, Iowa—The *Iowa* Section announces the election of the following officers for the 1952–53 season: *Chairman*—P. G. Parks; *First Vice-Chairman*—R. T. Rogers; *Second Vice-Chairman*—J. W. King; *Secretary*—L. F. Pettit; and *Treasurer*—E. A. Friedel.

Resistance Welding Machines

Detroit, Mich.—On Friday, November 7th, the *Detroit* Section enjoyed a very pleasant and profitable meeting held in the Engineering Society of Detroit Bldg. The dinner attended by 100 members was highlighted by the simple philosophy of W. A. Anderson AWS of the Taylor-Winfield Corp. His talk "Laugh it Off" included a well-provided repertoire of hilarious anecdotes which kept the members in a very good mood for the rest of the evening.

The technical talk was presented by J. H. Cooper AWS, Chief Sales Engineer of the Taylor-Winfield Corp., who by means of pictures presented a complete line of automatic machines and setups of his company. Following a period of questions and answers the members were treated to fine refreshments in the main dining room of the Engineering Society Bldg. through the courtesy of the Taylor-Winfield Corp. to which go the thanks of the Detroit Section.

Special commendation for seeing that the schedule of the program is meticulously maintained goes to Bob Bennewitz AWS.

Distortion in Welding

Erie, Pa.—The *Northwestern Pa.* Section held its regular monthly dinner meeting on November 11th at the General Electric Community Center. Robert E. Haas AWS gave a clear-cut talk which was accompanied by a motion picture film on the subject "Distortion in Welding."

Resistance Welding

Hartford, Conn.—An illustrated talk on "Basic Principles of Resistance Welding" was given by William J. Farrell AWS, Sciaky Brothers, Inc., at the dinner meeting of the *Hartford* Section held on November 13th at the Rockledge Country Club, West Hartford.

Pressure Vessel Design

Houston, Tex.—Dr. H. C. Boardman AWS, Director of Research, Chicago Bridge and Iron Co., Chicago, spoke before an attendance of 157 members of the *Houston* Section at a dinner meeting held on November 25th in the Ben Milam Hotel. Dr. Boardman's address on "Pressure Vessel Design—Now and in the Future" was illustrated with slides.

An after-dinner feature was the showing of a sound colored film "Migrants of the Marshlands" through the courtesy of the Humble Oil and Refining Co.

Silver Alloy Brazing

Indianapolis, Ind.—An excellent film and talk on "Design for Silver Alloy Brazing" was given by R. J. Metzler AWS, Development Engineer, Handy & Harman, New York, at the November 21st meeting of the *Indiana* Section held at Buckley's Restaurant, Cumberland. The motion picture film "Production Brazing with Low-Temperature Alloys" was shown through the courtesy of Handy & Harman.

Resistance Welding Developments

Kansas City, Mo.—T. Embury Jones AWS, President, Precision Welder and Machine Co., Cincinnati, Ohio, was the guest speaker at the regular monthly dinner meeting of the *Kansas City* Section held on November 13th in Fred Harvey's Pine Room. Mr. Jones's talk on "Developments in Resistance Welding" covered the history of the process from the time of its accidental discovery until the present. With the aid of slides he discussed present-day machines, giving their advantages and specific applications. Samples of welds made by this process were available for inspection after the meeting.

Before the technical session a color sound movie was shown entitled "Ozark Float Trip."

Aircraft Rocketry Confab

Los Angeles, Calif.—A review of Metal Show (Philadelphia, October 1952) features of interest to the *Los Angeles* Section Aircraft and Rocketry Welding Panel was given in Swally's Cafe, Los Angeles, on November 6th by Charles Smith AWS, Ludwig Kellerman AWS, Russell Meredith and Francis Stevenson AWS before 60 in attendance. A dinner preceded the meeting.

Titanium. Mr. Stevenson reported that problems originally encountered in fabricating the commercially pure titanium are now becoming a thing of the past. The present grades have excellent weldability characteristics. Good ductility may be obtained. The titanium alloy containing 7% managnese is also very weldable, but the resulting weldment is very brittle. Emphasis was shown in the inert-gas-shielded nonconsumable electrode process for joining titanium. However, a well known company in the East is doing a considerable amount of research work involving the use of the inert-gas-shielded consumable electrode process for joining titanium.

A huge roll of commercially pure titanium sheet, 0.0156 x 30 in. x 460 ft, was exhibited by a well-known titanium producer.

The latest and one of the most reliable heat treatments for the 7% manganese titanium high temperature-strength alloy is an equilibrant treatment of 1350° F, furnace cool to 1150° F and water quench.

Shielded-Metal-Arc Electrodes. Mr. Smith advised that the aircraft industry is confronted with a very grave problem concerning certain Army, Air Force and Navy specifications which allow engineers to design to very low strength allowables, specifically in welding heat-treatable steel alloys, such as SAE 4130 and 4140. The design standard permits a mediocre 85% of 125,000 psi ultimate design allowable for heat-treated weldments, unless an extensive series of tests are made to substantiate higher values in each individual deviation. This is a detriment to the strength-weight problem. It only allows engineers to build aircraft and rocket parts with a terrific weight penalty.

In an effort to bring this strength allowable up to a much higher value, several aircraft representatives have made painstaking research studies of available commercial electrodes. They have found that there are several electrodes on the market

that will give consistent high-strength tensile values after heat treatment.

The results of these studies have been sent to Washington, D. C., for review in an effort to have the Armed Forces increase the design allowable.

In a reply to this request a Pentagon representative said that an investigation revealed that out of several electrode manufacturers only one electrode manufacturer would acknowledge that their high-strength, heat-treatable electrodes would comply with the proposed specification requirements.

The problem that now exists is to convince other electrode producers that they must develop new electrodes that would give the high strengths required by aircraft users and provide applicable process specifications.

Apparently the electrode producers are content with manufacturing just mild steel electrodes because the demand is more than for heat-treatable electrodes.

Technical and Operational. A general discussion followed. A general rule of the thumb for determining what ductility exists in welded commercially pure titanium sheet (butt joint) can be estimated by the temper colors of the weld and heat-affected area. Tom Bradley stated that three distinct colors have been identified by his company; straw, peacock and blue. The color density is most likely in direct relation to the valence change of the titanium oxide. As the color changes from a shiny metal luster to straw, peacock and then blue, the ductility decreases proportionately. The parent metal ductility may be obtained by removing these temper colors chemically.

John Arthur AWS opened a discussion by asking, "Who uses welding symbols and what is their significance to aircraft engineering?"

The first question was answered by a show of hands. The following companies apparently use welding symbols in one form or the other: Solar Aircraft Co., North American Aviation, Downey Division, Douglas Aircraft, El Segundo, Consolidated Western Steel, Aerojet Engineering Corp. and the petroleum industry.

In boiling down the hotly discussed portion of the second part of the original question, two sides were taken; one faction maintained that the welding symbols should be changed to comply with aircraft materials and the other faction stood their ground by maintaining that the AWS welding symbols, as devised by the AWS technical committee, were sufficient for all known needs. The majority of the panel agreed that the symbols were a great help for engineering as well as operational needs in any industry. However, because each company has adopted different process specifications, the vendors apparently have a great deal of trouble in maintaining a precise file and working knowledge of each different specification. It is their wish that a standard be adopted by all companies.

New Members. The following new members were introduced to the panel: Byron Fry, Albert Pardoe, Paul Snyder and Joe Marden (transferred).

Special Events

Milwaukee, Wis.—The *Milwaukee* Section will hold its educational course on Monday evenings—January 12th, 19th and 26th, also on February 2nd. The Section's annual party will be held on May 16th.

The Arcair Process

Peoria, Ill.—The *Peoria* Section held its regular monthly dinner meeting on November 19th at the Mecca with an attendance of 38 at dinner and 40 at the meeting. "The Arcair Process—Gouging with the Electric Arc and High Velocity Jet of Compressed Air" was the subject of an extemporaneous semitechnical talk given by M. D. Stepath AWS, President, Arcair Co., Bremerton, Wash. Mr. Stepath's very interesting discussion was illustrated with slides and was followed by a 30-minute question period. Technical Chairman of the meeting was E. F. Baker of the Caterpillar Tractor Co.

Submerged Arc Welding

Philadelphia, Pa.—Norman G. Schreiner AWS, Linde Air Products Co., gave an extemporaneous discussion on the subject "New Developments in Submerged Arc Welding" at the dinner meeting of the *Philadelphia* Section held on November 17th at the Engineers Club. Mr. Schreiner used slides to illustrate his talk.

Avoiding Trouble

Phoenix, Ariz.—A very interesting monthly meeting of the *Arizona* Section was held at the Hotel Westward Ho, Wednesday, November 9th.

Following the dinner, attended by 40 members and guests, a fine talk was given by Charles F. Carlson AWS, Superintendent of the Blacksmith Shop of Allison Steel Manufacturing Co., who spoke on "What Would You Do"? He pointed out problems and solutions of shops that rarely appear in reference books. All agreed it was most informative.

The next speaker of the evening was Charles S. Fogwell AWS, owner and operator of the Haywire Welding Works of Phoenix. His talk entitled "Bad Weld" was indeed unusual both in presentation and in substance. Samples of bad welds were shown to the membership and means of how to avoid them were discussed. Mr. Fogwell's wit further enriched his talk as he told of some of his actual experiences in welding over the past 20 years.

The Section was very grateful to have these two men speak to it and hope they will do so again in the near future.

Plant Tour

Rochester, N. Y.—The Foster Wheeler Corp. invited the members of the *Rochester* Section to Dansville on October 13th for a tour of their plant. The tour was preceded by a dinner served at the Dansville Moose Club, compliments of the Foster Wheeler Corp., which about 150 men agreed was second to none.

Chairman Grover Stelljer opened the meeting and introduced F. F. Godlesky, Works Manager of Foster Wheeler Corp., who welcomed the group and expressed his desire for all to have an enjoyable and interesting evening. L. E. Conklin AWS followed with a few remarks on the events for the evening. N. N. Martin AWS, Welding Technician, gave a brief but interesting history of the progress of welding and the part that it played in the fabrication of equipment produced by Foster Wheeler Corp.

The plant tour was conducted by well-informed guides, who explained the purposes of the many fabricated parts used primarily in oil refineries. The group saw practical application of submerged arc, automatic butt welding by the oxy-acetylene method, precision cutting and pipe bending by way of induction heating.

The Rochester Section wishes to thank Foster Wheeler for a very informative and entertaining program.

Plant Visit

Saginaw, Mich.—On November 13th a group of 90 members of the *Saginaw Valley* Section had dinner and visited the plant of the Saginaw Malleable Foundry Division, General Motors Corp.

Oil Pipe Line

San Francisco, Calif.—Two features were scheduled for the dinner meeting of the *San Francisco* Section held on November 24th at the Engineers Club. The first was the showing of a new 22-minute sound and color film presented by Charles Robinson AWS, Special Technical Representative of Air Reduction Pacific Co.

The second feature was the showing of a 36-minute color and sound film entitled "Kirkuk to Banias" by Richard Finney, Historian of Bechtel International Corp., and Russell G. Rhoades, Welding Engineer, Bechtel Corp., San Francisco. This film shows the construction of 556 miles of 30- to 32-in. diam crude oil pipe line, which started in Iraq and terminated on the Mediterranean Coast of Syria. It was built for the Iraq Petroleum Co. and passes through lands of ancient historical interest, including the city of Palymira and the Crusade Castles. The

film highlights the equipment and the technique used in building a modern pipe line in such a remote area. Preceding the showing of the film an article written for *Oil and Gas Journal* was read.

Low-Hydrogen Electrodes and Hard Facing

Seattle, Wash.—A discussion on low hydrogen by representatives of the Reid Avery Co. and a hard facing discussion by representatives of the Linde Air Products Co. formed the basis of the dinner meeting held on November 13th by the *Puget Sound* Section at the Engineers Club, Seattle. Speakers were as follows: S. Viggers AWS, Acme Boiler; J. Whiteside, Naval Inspection; M. Crawford AWS, Boeing Airplane Co.; Prof. Schaller, Univ. of Washington; W. Ross, Arcair Co.; J. Dickinson AWS, Linde Air Products; Christianson, Vice-President, Reid-Avery Co.; Tibbets, Welding Engineer, Reid Avery Co.

Pressure Piping

Sheffield, Ala.—A meeting of the *Tri-State* Section was held on October 3rd at Muscle Shoals Hotel. The meeting was opened by Jack York, Chairman, who also gave a talk on the purpose of the AWS and the benefits to be gained through membership in the SOCIETY.

The principal speaker was E. R. Seabloom, First Vice-President, AMERICAN WELDING SOCIETY, and Supervisor of Engineering with the Crane Co., Chicago, who gave a practical review of carbon and alloy steel pressure piping. Over 100 slides were shown covering section of piping materials being subjected to laboratory tests. Also shown were field failures from joint design, and graphitization. After the talk the meeting was thrown open to discussion.

New Member Night

Syracuse, N. Y.—The *Syracuse* Section held its New Member Night on November 12th in the Hiawatha Room of the Onondaga Hotel. Approximately 20 new men who joined the Section this season were introduced by Membership Chairman Jim Solar.

I. Morrison, national director of the AMERICAN WELDING SOCIETY, gave a personal account of how membership in the SOCIETY has helped him in his work and improved his business. Simon Greenberg, Technical Secretary, described the SOCIETY as an excellent source of technical data, told of the work being done in establishing welding standards and codes and cited the useful personal experience one may have by becoming active in the SOCIETY. Bob Greer AWS, Chairman of the Local Industrial Service Committee, told how the Section was founded with eight members ten years ago and then steadily grew to the present number of 150.

Technical speaker was Julius Heuschkel AWS, Research Engineer for Westinghouse Corp., who spoke on "Cracking in Welding."

The technical highlights of Mr. Heuschkel's talk are as follows:

1. Certain materials tend to show localized intergranular melting (burning) or excessive grain boundary precipitation (overaging) near the fusion zone of the weld.

2. This grain boundary condition presents planes of weakness or low ductility and, when the welds are stressed during the cooling cycle or in service, cracks may develop.

3. It is possible to reduce this intergranular weakness by controlling chemical composition. For example, carbon should be kept low in stainless steel, sulfur and phosphorus minimized in mild and alloy steel, and sulfur and lead avoided in nickel alloys.

4. However, it is not possible to eliminate intergranular weakness entirely. In fact, under some welding conditions, cracks may develop in materials normally considered ductile.

5. Therefore, it is necessary to reduce stresses in the weld area. Factors to be considered are rigidity of the members, joint design, materials, welding process and welding sequence.

6. Preheat is helpful in reducing the critical cooling rate which, in turn, reduces the hardness and localized stresses in or near the weld. Preheat in the vicinity of 500–600° F was preferred for medium carbon (approximately 0.45) steels. Lower preheat in the neighborhood of 200° F would not be especially beneficial.

7. Since the factors involved in weld cracking are complicated, it is suggested that after careful analysis of the factors mentioned above, actual welding tests be performed prior to committing a design to full-scale production.

The meeting was adjourned in time to watch television and see a Syracuse boxer bounce off the canvas three times. However, the coffee and sandwiches were good.

Sigma Welding

Washington, D. C.—H. T. Herbst AWS, Linde Air Products Co., presented an illustrated talk on "Sigma Welding of Carbon Steel" at the October 22nd meeting of *Washington* Section held in the Washington Post Auditorium. Dinner at the Good Earth Restaurant preceded the technical meeting. After the meeting pie alamode and coffee was served. It was agreed that this part of the meeting was in the atmosphere of a club meeting and should promote good relations.

Resistance Welding

Wichita, Kan.—Over 50 members were present at a meeting of the *Wichita* Section held on November 10th at the Cessna Aircraft Co. A plant tour through the Cessna Aircraft Co. preceded dinner in the cafeteria. T. Embury Jones AWS, President, Precision Welder & Machine Co., Cincinnati, Ohio, gave an excellent semitechnical talk on the subject "Resistance Welding," which was of general interest to all.

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abstracts of
CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25c from the Commissioner of Patents, Washington, D. C.

2,614,517—BRAZING JIG FOR FINNED HEAT EXCHANGER ENVELOPES—David G. Peterson, Wellsville, N. Y., assignor to The Air Preheater Corp., New York, N. Y.

Peterson's patent is on a specialized brazing assembly jig for a heat exchanger envelope that includes spaced rectangular wall-forming metallic plates provided on

their outer surfaces with longitudinally extending metallic fins.

2,615,110—Dirigible Automotive Arc Welder—Ernest W. Gommel, El Cerrito, Calif., assignor to United States Steel Co., a corporation of New Jersey.

The patented arc welder includes a motor housing having support wheels adapted to travel on a workpiece. A motor is positioned in the housing and drives the wheels, while a hopper is provided on one side of the housing for depositing welding flux on the workpiece on a line between the wheels. An electrode guide tube extends through the flux deposit spout and a manipulating handle is provided for such guide tube so that the tube may move toward or away from the workpiece by tilting the manipulating handle to cause the electrode to move toward or away from the workpiece, as desired.

2,615,236—Blade Edge Welding Technique—Walter H. Stulen, Caldwell, Mathias Klein, Hawthorne, and Edwin L. Tichenor, Montclair, N. J., assignors to Curtiss-Wright Corp., a corporation of Delaware.

This patent is on a method of joining two acutely angled metallic members in edge-to-edge relation. The edges of the members are prepared by grooving an inside edge corner of each and assembling the members in edge-to-edge relation with the inside corner grooves cooperating to form a larger groove in the assembled unit. The members are welded along the edges from the side thereof opposite the sides carrying the corner grooves to a depth substantially reaching the bottom face of the larger groove. The final operation is to move a cutting tool along the sides of the members containing the groove and piloting this cutting tool in the groove to enlarge and deepen the groove until the cutting tool enters into the welded metal joining the members together.

2,616,014—Weld Analyzer—Robert O. Ellerby, Bloomfield Hills, Mich., assignor to General Motors Corp., Detroit, Mich., a corporation of Delaware.

The patented weld analyzer relates to indicating means for determining the different varying factors in the normal operation of a complex machine throughout its cycle of operation. The analyzer includes means for translating operating factors into proportionate electrical voltages and means for varying the electrical circuit connected to the proportionate electrical voltages as the machine factor changes. A cathode-ray tube is connected in the control means for indicating changes in the machine factor.

2,616,015—Machine for Welding Battery Plates—Carl R. Glasener, Cleveland, Alfred R. Willard, East Cleveland, and Carl R. Weller, Cleveland, Ohio, assignors to Willard Storage Battery Co., Cleveland, Ohio, a corporation of West Virginia.

This patented machine relates to the welding of connectors to battery plates and it includes means for forming a guideway adapted to receive plates to be welded and an electrode of a welding circuit associated with the guideway for contacting a plate therein. Mechanism is provided in the apparatus for feeding a connector into position to be welded to a plate in contact with the electrode, and reciprocating means move plates along the guideway and position them in contact with the welding electrode. A second welding electrode is adapted to be moved into engagement with the connector on the plate, and the reciprocating means, connector feeding mechanism and movable electrode of the apparatus are operated in synchronism.

2,616,016—Rotary Electric Resistance Welder—William E. Shenk, Hubbard, Ohio, assignor to The McKay Machine Co., Youngstown, Ohio, a corporation of Ohio.

Shenk's rotary electric resistance welder has a centrally disposed shaft for the support of a transformer and electrodes and a supporting bearing positioned axially intermediate the transformer and electrodes. Means are provided for mounting a pair of wheel type electrodes on the end of the shaft and including a pair of disk-like mounting and conducting members seated on the shaft. The axially innermost of the disk-like members has a plurality of circumferentially spaced apertures therein and metallic current conductive tubes are positioned in the apertures. The axially outermost of the disk-like members has a plurality of outwardly tapering apertures formed therein aligned axially and circumferentially with the tubes. Current conductive rods are disposed concentrically within the tubes and annular, current conductive wedges surround the end portions of the rods and are partially received within the apertures in the outermost of the members. Screw means are provided on the ends of the rods for engaging the outer ends of the wedges to draw the wedges inwardly while placing the rods under tension in the welder apparatus.

2,616,017—Electrode Holder for Use in Inert Gas-Shielded Arc Welding—Nelson E. Anderson, Berkeley Heights, N. J., assignor to Air Reduction Co., Inc., a corporation of New York.

The novel element in this patent relates to means for delivering a shielding gas around the tip of the electrode. Specifically such means include a tubular metal core having an interior and exterior of highly refractory vitreous enamel material.

2,616,489—Underwater Cutting Torch—Thomas J. Skailes, Baraboo, Wis., and Raymond R. Zimmer, Chicago, Ill., assignors to The Bastian-Blessing Co., Chicago, Ill., a corporation of Illinois.

The patented torch has a plurality of conduits, a handle on the conduits, and means for connecting the conduits to separate sources of gases. The patent discloses use of means for controlling the flow of gas through one of the conduits and including a valve and a valve-operating lever assembly disposed on one side of the handle where an operator's hand holding the handle can control the valve-operating lever assembly. This valve assembly includes special means for controlling the flow of gases through the various conduits provided in the torch.

2,617,003—Method of Automatic Electric Arc Welding of Workpieces—Henry Bienfait and Simon Dirk Boon, Eindhoven, Netherlands, assignors to Hartford National Bank & Trust Co., Hartford, Conn., as trustee.

The method disclosed in this patent relates to automatic electric arc type welding of workpieces by means of a plurality of coated self-starting touch-welding rods and comprises the steps of placing a first welding rod with one end thereof in welding position with relation to the workpiece, a second welding rod with one end thereof in welding position with relation to the workpiece and insulated therefrom. A common welding current is connected to both welding rods and to the workpiece to form a welding arc between the first rod and the welding piece. The first welding rod is used until it has consumed a desired amount and then the remaining portion of the first rod is spaced from the workpiece to increase the length of the welding arc and thereby increase the arc voltage until the arc voltage attains a value sufficiently high to break down the insulation between the second rod and the workpiece and so start the welding arc between such second rod and said workpiece.

2,617,004—Electric Flash Welding—William C. Heath, Milwaukee, Wis., assignor to A. O. Smith Corp., Milwaukee, Wis., a corporation of New York.

In Heath's electric flash-welding process, he forms parts of the articles to be welded from low alloy steel containing a small percentage of one or more of the following ingredients: vanadium, molybdenum and chromium. A seam is electrically flash welded between two of such low-alloy steel parts and such seam has the flash cleaned from both surfaces thereof.

2,617,643—Surface Hardening Machine—Richard J. Clarcq, Jr., Rochester, and Carl E. Ernst, Brighton, N. Y.,

assignors to Gleason Works, Rochester, N. Y., a corporation of New York.

This patent on a surface-hardening machine particularly relates to the hardening of gear teeth. A work support is provided in the machine, as is a reciprocable ram which positions a pair of laterally spaced torches thereon for directing flames on opposite tooth sides of the work. A temperature-sensitive element is mounted on the ram laterally between the two torches to be adjacent the tooth being hardened to sense the part of the tooth being heated by the torches and measure the temperature thereof. Means are operatively connected to the temperature-sensitive element for controlling the rate of movement of the ram for controlling the surface-hardening action.

2,617,644—Universal Gas Torch Cutting Machine—Richard F. Helmkamp, Jersey City, N. J., assignor to Air Reduction Co., Inc., a corporation of New York.

In this universal gas-cutting machine, a monorail support is positioned horizontally and has a curved top surface. A carriage is mounted on the monorail support and has laterally spaced wheels for contacting and rolling along the curved top surface of the monorail at opposite sides of the crest thereof. A transversely extending bar is carried by the carriage which can be shifted laterally on the top surface of the monorail for varying the bars position. A tracer is carried by the bar at one side of the carriage and has a power-driven traction wheel for rolling on a surface containing a pattern to be traced by the tracer, and a torch is carried by the bar at the other side of the carriage and is free to move toward and away from the surface of the work to permit rocking of the bar in a vertical plane.

2,617,913—Low Open-Circuit Voltage Alternating-Current Welding Circuit—Sol Oestreicher, Milwaukee, Wis., assignor to Harnischfeger Corp., Milwaukee, Wis., a corporation of Wisconsin.

This alternating-current welder includes a transformer with a primary and a secondary winding and with the welding circuit being connected to the secondary of the transformer. A power supply is connected to the primary of the transformer and a primary circuit voltage reducing impedance is connected in the primary circuit while a primary circuit contactor having an actuating coil, a set of reduced voltage contacts and a set of welding voltage contacts also is connected in the primary circuit. Reduced voltage circuit connections join the primary winding through the reduced voltage contacts when closed, and in series through the voltage reducing impedance to the supply terminals of the transformer. Welding voltage circuit connections join the primary winding through the welding voltage contacts when closed directly to the supply terminals. In the secondary circuit, welding current responsive means are provided to energize the coil of the primary circuit contactor to cause the welding voltage contacts only to be closed when current is flowing in the secondary circuit and they are adapted to close the reduced voltage contacts only when current is not flowing in the secondary circuit.

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Supplement to the Welding Journal, January, 1953

Evaluation of Brittle Failure Research

Significance of transition temperature as determined by various specimens and criteria. Highlights of Research covering various factors on the transition temperature of ship steels

by E. F. Sweeney

Abstract

The paper presents an evaluation of brittle failure research from an engineering viewpoint. Transition temperatures for various specimens and criteria, and their significance, are discussed. Old and new ABS steels are compared, including the plates in which service failures originated. There are sections on the published research covering the effects of fatigue, residual stresses, cold work, heat treatment, chemistry and finishing temperatures on the transition temperature of ship steels. Highlights of the research covering weldments and design are included.

INTRODUCTION

THIS paper is written from a ship construction viewpoint but it should be pointed out that the problem of brittle fracture is not confined to ships. Other structures, such as pressure vessels, storage tanks, bridges and pipe lines have also suffered brittle casualties under loadings normally considered within their safe loading ranges.

A large amount of research work on the brittle failure problem has been reported in the technical literature. There are literally hundreds of articles bearing on various aspects of the subject, not all of them in agreement. The multitude of specimens and criteria used to determine performance makes it difficult for the engineer who is actively interested to keep abreast of current thought and progress in this field.

The purpose of this paper is to present an evaluation of brittle failure research from an engineering viewpoint. It is hoped that it will be of interest to the practical engineer and designer of welded structures. The paper may also be of interest to the scientists and investigators, as it should give them an opportunity to discover how much of their findings the engineer has assimilated or perhaps misinterpreted.

TRANSITION TEMPERATURES AND THEIR SIGNIFICANCE

There are four fundamental variables that affect the plastic behavior of steel:

1. State of stress (uniaxial, biaxial and multiaxial).
2. Strain rate.
3. Material quality (chemical composition and microstructure).
4. Temperature.

To assess the susceptibility of a given steel to brittle fracture, variables 1, 2 and 3 are held essentially constant by selecting a specimen type and method of loading for a given material. A series of these identical specimens is then tested at successively lower temperatures and a measure of the energy absorption, ductility, strength or fracture appearance recorded for each specimen. A temperature range will be reached where the value of the selected criterion will drop off to lower

E. F. Sweeney is connected with the Shipbuilding Division of the Bethlehem Steel Co., Quincy, Mass.

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Table 1—Various Criteria for Transition Temperature

Energy	*Ductility*	*Strength*	*Fracture appearance*
Total energy absorbed	Bend angle at maximum load	Yield to ultimate ratio	Shear, %
Maximum slope			
One-half maximum energy			
Energy to maximum load	Reduction of area		
Energy after maximum load		Nominal strength	
The scatter range with minimum number of values	Contraction below notch root		
A selected low level of energy absorption	Bend angle		

values with lower temperatures. A temperature within this range is selected as the transition temperature. (See Table 1 for various criteria for determining the transition temperature.) The majority of those interested in the brittle fracture problem prefer energy or ductility measurements as criteria of brittleness.[1]

If a second steel is tested, using the same type specimen, method of loading and criterion, its susceptibility to brittle failure can be judged relative to the first steel. Rate and type of loading are kept the same because there is a nonlinear relationship between strain rate and temperature.[2] Increasing the strain rate has the same effect as lowering the temperature.

Most specimens contain a notch in order to obtain a severe enough stress state so that the transition temperature will occur in a range of practical temperatures, both for testing convenience and because brittle fractures originate at notches in practical structures. Hence the term "notch sensitivity" of steels. This is at best a poorly defined quantity because so many different specimens and criteria are used.

All of these specimens are valuable laboratory tools and will, in a general sense, rate steels in the same order of merit. However, the correlation between them is not always good. The major reason is that different criteria are used to define transition temperature.

Stout and McGeady[3] sum it up well: "The choice of specimen is far less critical to correlation of test methods than is the choice of criterion used to establish the transition temperature. In any event, the identical criterion should be used in any such correlation. It is unreasonable to expect that energy measurements on one type of specimen should allow correlation with ductility measurements on a second specimen or with appearance of fracture on a third. They do not necessarily measure the same property of the steel, nor may all of them measure the property of real significance."

The criteria for determining transition temperature fall roughly into two groups. The first group determines the temperature at which the major portion of the fractured surface changes from a shear to a cleavage type failure. This is called the fracture transition (T_B). It is a measure of the temperature at which a crack already started will propagate rapidly in a brittle manner with little energy absorption at the stress level and under the stress system in the specimen. The fracture transition is relatively insensitive to the notch sharpness or specimen design. The Kahn[4] tear test and the wide-notched plate tests[5] are in this category, as are all large test specimens whether the criterion used is fracture appearance or one-half the maximum energy absorbed.

The second group determines the temperature at which ductility in the immediate vicinity of the notch root is severely restricted. This is termed the ductility transition (T_N). It is a measure of the temperature at which the energy absorption falls to low values in the critical area at the notch root before cracking becomes appreciable. The many small, sharply notched specimens are in this category provided a proper criterion is used, such as energy to maximum load, bend angle at maximum load, percent contraction in thickness at the notch, or, if total energy has been measured, selecting some arbitrary low level of energy absorption.

Idealized transition temperature curves are shown in Fig. 1. The solid curve represents a specimen and steel which shows a sharp drop in energy absorption at T_N as well as at T_B. Other specimens and steels have performance curves more like the dashed curve and T_N is determined by some low level of energy absorption.

In the temperature range between T_B and T_N, a specimen will exhibit considerable local energy absorption immediately adjacent to the notch before cracking becomes appreciable. Once the crack has opened up, however, it will propagate in a brittle mode with little energy absorption. Below T_N the crack will be brittle from its inception.

The ductility transition (T_N) is quite sensitive to specimen design particularly notch sharpness. As the notch becomes more severe,[6] T_N moves to higher temperatures and approaches nearer to T_B.

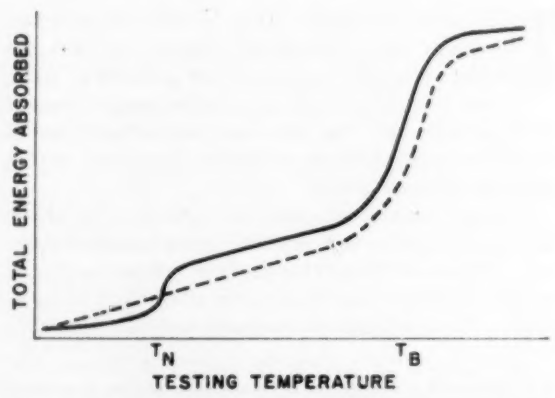

Fig. 1 Schematic transition temperature curves (after Vanderbeck & Gensamer[4])

Since T_N is below T_B, it is harder to start a crack than to propagate one. An excellent paper by Irwin and Kies[7] states this in a more direct way. They report that in size-effect studies the work done (or the energy absorbed in plastic flow) to initiate a crack in notched steel bars was proportional to the cube of the test piece dimensions. As the cracks propagated, however, the work done dropped off toward proportionality to the fractured area.

Because of the inherent scatter of the experimental values around the smoothed transition temperature curve which is drawn through them, no matter what specimens and criteria are used, it is only possible to speak of a transition temperature in a statistical sense. In addition to the most probable value some measure of the spread is desirable. Charpy V-notch transition temperatures have been shown to vary by 20 to 25° F for specimens from the same ingot, and by 50° F from the same heat, both for a rimmed and for an aluminum-killed steel.[8] Other tests[9] show a 40 to 50° F variation in bars taken from locations 10 ft apart in the same plate.

A comprehensive study[10] established the transition temperature scatter for Charpy bars from 15 laboratory heats in terms of standard deviation (see Table 2). Further work showed that changes in composition did not greatly affect the scatter so that these data can be used generally.

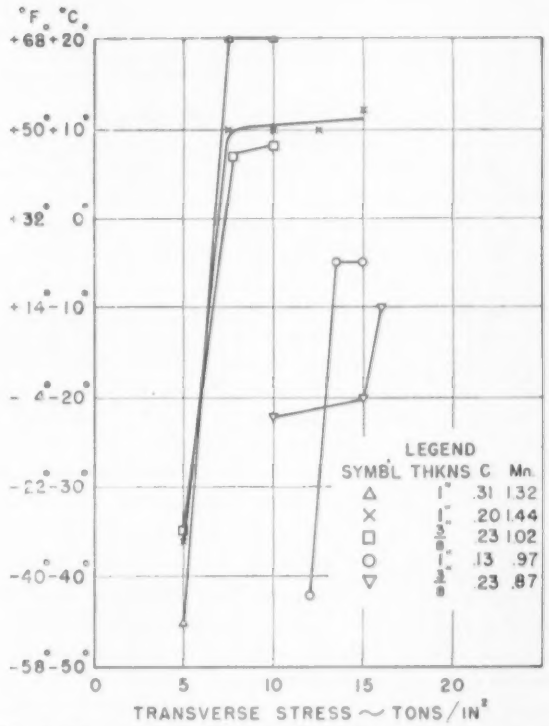

Fig. 2 Temperature of arrest of brittle crack vs. transverse stress (data from Robertson[12])

Table 2—Transition Temperature Deviations of Charpy Bars

Specimen type	*Definition*	*Standard deviation, ° F*
V-notch	15 ft-lb, av. energy, 50% shear	8.2
	first sign of cleavage	6.7
		14.0
		20.4
Keyhole notch	Middle of scatter zone, 20 ft-lb	9.0
		11.6

RELATIVE SIGNIFICANCE OF TRANSITION CRITERIA

The question of the relative significance of the fracture transition and the ductility transition as measures of service performance has not been resolved. There are conflicting opinions. To the engineer, this is the most important undecided question in brittle failure research. Until it is resolved, he does not know what yardstick to use in determining whether a material is suitable for his purpose and in some cases does not even know trends. The two criteria rank steels in somewhat different orders and it has been shown[11] that 1200° F postheat can lower T_N but raise T_B. In the interim the engineer must draw his own tentative conclusions.

No one advocates use of a steel that would cause the critical areas of a structure to operate below T_N.

At the other extreme, there can be little doubt that if a steel were used that would enable all parts of a structure to operate above T_B under all service conditions, the problem of extensive brittle fracture would be solved by materials alone and there would be no limitations or restrictions imposed on the designers. The questions are whether or not such a solution can be achieved within economic limitations and if such a complete solution is necessary from a ship fracture standpoint.

The fracture transition temperature (T_B) as determined by wide flat plates[5] seems to be somewhat high because their nominal stress level at fracture is of approximately yield point magnitude, or greater, which is higher than ship operating stresses.[13] In a ship there would be danger of buckling before these stress levels were reached.

The still water bending moment stress on a ship due to loading might be four or five tons and the seaway stress possibly another three or four so that a 10,000 to 20,000 psi nominal stress might be expected but not values of 30,000 psi. It has been shown[12] that the temperature at which a brittle crack will propagate is dependent on the stress level (see Fig. 2).

Since this type of test is probably not as well known as most, the details will be outlined (see Fig. 3). The test piece is 3 in. long and 12 in. wide with a hole drilled in one end. At the side of the hole next to the main body of the specimen a jeweler's sawcut is made. Strips of thinner material are welded to the long edges of the specimen and the assembly is then loaded beyond the yield point of the thin strips to insure uniform

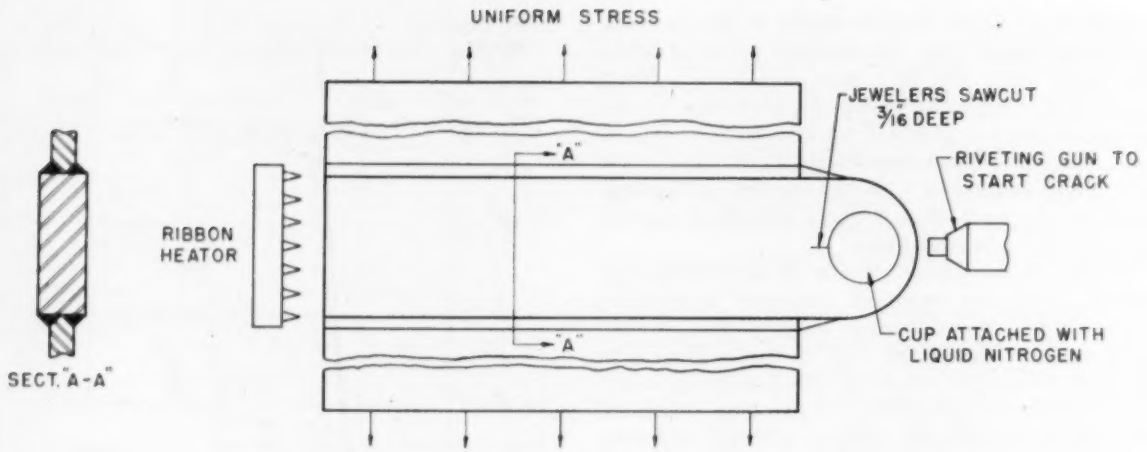

Fig. 3 Robertson test specimen (schematic)

stress distribution. After the mechanical stress-relieving process, extensometers are attached, load is applied and the stress and its uniform distribution are verified. Brass cups are attached to each side of the drilled hole and liquid nitrogen is poured into one of these at a regulated rate. As it evaporates it passes through the hole and out through a vent in the other cup. The other end of the specimen is heated with a flat flame gas burner.

When the temperature gradient has reached a steady value the temperature along the specimen is measured by thermocouple and recorded. The desired load is applied and while a heavy mass is backed up to the heated end, a rivet gun is positioned to deliver an impact to the cold rounded end.

A crack is started at the notch by the impact and propagates rapidly along the length of the specimen into zones of gradually increasing temperature until it stops. The speed of propagation is so rapid that high-speed photography at 4000 frames per second cannot record it. At such a high speed of propagation there is not time for the load to shift from the cracked to the uncracked portion and increase the tension. As a result the nominal stress in the material through which the crack propagates must remain essentially constant. Soon after the crack arrests, an increase of stress follows and extensive yielding at the root of the crack occurs.

The temperature at which the crack arrests is determined by cutting open and examining the specimen. Where the brittle fracture stopped there is a characteristic thumbnail marking, the focus of which is used to identify the temperature at which arrest took place. Consistent results were obtained for the same material at the same stress level but with different temperature gradients.

Results from this type of test are in terms that a designer can use directly. For a given material and thickness this test gives a minimum temperature for a given stress, or a maximum stress for a given temperature, at which a high velocity brittle crack will be arrested.

Tests of this type on commercial ship steels would help to clear up the significance of the fracture transition temperature. This test may be too severe for there may be some "coasting" before the crack arrests. The effect of length should be investigated also, for only if a sufficiently large reservoir of elastic strain energy is readily available without appreciable inertia can a brittle crack be propagated rapidly. H. DeLeris[7] could not obtain a brittle fracture at room temperature until he lengthened his specimen.

The stress level is important because the stored elastic strain energy is proportional to the square of the stress and it is the released elastic strain energy which propagates the fracture.

A survey of the thickness reduction at the fractured edges of plates from ship casualties showed up to 4% thickness reduction which, when correlated with other test results,[14] suggested that the energy absorbed, and therefore the released elastic strain energy available to propagate the cracks, may have approached 4000 in. lbs per sq in. However, the larger percent reductions were in through or end plates[18] and not the source plates (see Table 3). As far as source plates are concerned, this 4000 in. lbs per sq in. figure appears to be high.

The trend shown by these fractured ship plates (less energy absorption near the source of fracture) agrees with other data which shows that the amount of elastic strain energy released per inch of crack is a minimum at the start of a crack and increases with crack length (see Fig. 4). Absolute values will depend on the stress level, the physical dimensions and the axial rigidity of the test piece.[7] The curves given in the figure are for a simple strip with an axial elliptical hole. The axial rigidities of practical structures have not been derived but the trend shown is important: for a given stress level, the released elastic strain energy available to propagate a crack is a minimum at the crack source.

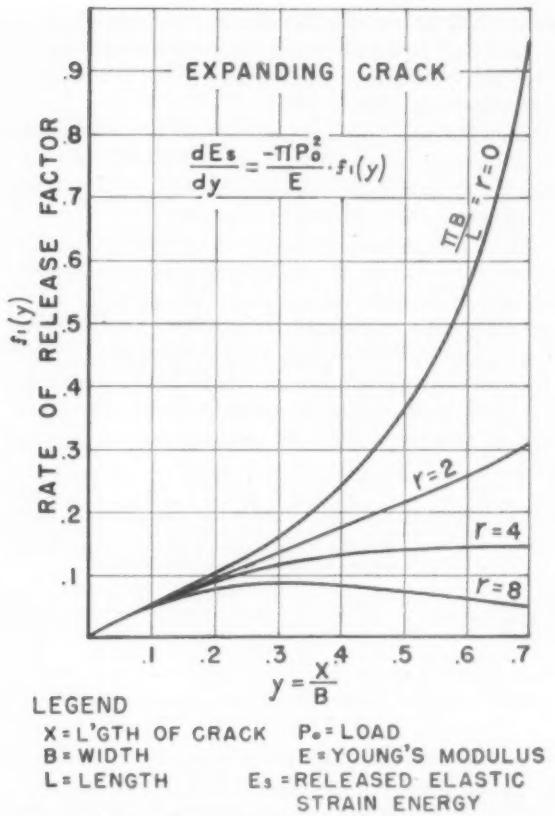

Fig. 4 Rate of Release of stored elastic strain energy (After Irwin & Kies)

The above line of reasoning, coupled with the fact that the available data shows ship fractures occurred with very small strains at the fracture source, indicates that the failures occurred because the critical area was unable to absorb a relatively small amount of energy. This points to the ductility transition as being significant in evaluating service performance.

If the fracture transition temperature were the significant criterion of service performance it would be expected that a substantial number of the service failures would have occurred with appreciable plastic strain and percent thickness reduction at the failure source.

To depend on the ductility transition completely would be dangerous because it is sensitive to various operations, such as welding or cold work and strain aging. Sharpness of notch and degree of triaxiality also affect it. Increasing the severity of any of the above shifts the ductility transition to higher temperatures.

It appears that the most feasible interim interpretation that the engineer can make is that if the critical areas of his structure operate just above, rather than just below, the ductility transition temperature, the probability of brittle failure will be greatly reduced, but not necessarily to an acceptable level.* A somewhat greater margin of safety is needed so that if the ductility transition has been raised locally, or a minor crack exists, the amount of plastic strain energy that the material can absorb will be greater than the amount of elastic strain energy released. This would prevent an energy unbalance and rapid propagation of a crack.

Just how high above the ductility transition the criterion should be set is a moot question. It will depend on the general stress level and the care with which detail design and fabrication is carried out to ease notches and stress concentrations. Material quality, design and fabrication are interrelated. A plus value on any one factor will help to mitigate a deficiency in either of the other two. In any event the acceptable criterion should be high enough to tolerate minor cracks.

The general picture appears to be this: if the critical areas of structures operate below the ductility transition there will be some failures no matter how careful the design of details and fabrication; if they operate just above the ductility transition the probability of failure will be greatly reduced but not necessarily to an acceptable level; as the operating temperature increases further above the ductility transition, the probability of brittle failure decreases rapidly toward a very small number provided design and fabrication are good; finally, as the operating temperature approaches the fracture transition, the chance of failure is remote even if design and fabrication are not particularly good.

That changes in detail design can result in tremendous improvement is shown by the service records of the original Liberty and Victory Ship hatch corner designs[15] (see Table 4).

Table 3—Fractured Plates from Ship Casualties

Plate Thickness, in.	*Thickness reduction at fractured edge, %*		
	Source plates	*Through Plates*	*End Plates*
0.44–0.69	1.1	2.5	3.0
	1.4	3.5	3.2
	2.2	3.5	3.4
	...	...	3.9
0.70–0.80	0.5	0.3	0.5
	0.5	1.0	1.1
	0.7	1.4	1.8
	1.3	1.6	2.8
	...	...	3.6
0.81–1.27	0.8	0.4	2.2
	1.5	1.0	2.2
	1.7	1.6	
	1.9		
	2.7*		

* Actual source not examined. Reported as fatigue crack at end of longitudinal. (From Williams, *et al.*)

* In a strict sense it is not possible to speak of eliminating the probability of brittle failure. Certainty can be approached but never achieved.

Table 4—Hatch Corner Service Records

Type	*Original Liberty design*	*Victory design*
Approximate ship years of service	2000	2000
Number of cracks reported	224	1

The steel that was used for both of these designs was ordered to the same specifications. While it may be argued that the average workmanship may have improved on the Victory ships the only deliberate change was in design. The original Liberty hatch corner design had many superimposed notches whereas the Victory Ship design eased or eliminated them.

The improvement in service performance of the Victory ship hatch corner over the original Liberty type parallels the improvement in energy absorption of corresponding full-scale hatch corner test specimens[16] (see Table 5).

Table 5—Energy Absorption of Full-Size Hatch Corner Specimens

Specimen Type	Energy absorbed
Original Liberty, in.-lb	230,000
Victory, in.-lb	5,800,000

This correlation between energy absorbed in test specimens and service performance underscores an important point brought home to the engineer by brittle failure research that even though service failures were low load failures what is needed is design for greater energy absorption rather than design for greater strength.

CORRELATION OF TESTS RESULTS WITH STRUCTURES

It has not been possible to achieve a precise quantitative relationship between the properties of steels in any of the many tests and their susceptibility to brittle fracture when fabricated into a structure.[17] This is not surprising when it is remembered that service conditions and the effects of welding and of fabrication as well as the material have statistical variations. At the same time it is not certain that the property of real significance only is being measured. All of the tests, however, give relative values from which significant information can be drawn.

An empirical correlation which cannot be ignored has been obtained between fractured plates removed from welded ships and 15 ft-lb V-notch Charpy bar transition temperatures.[18] The plates in which failures started had a higher average transition temperature than the through or end plates. This difference is highly significant (see Table 6).

Table 6—Average Transition Temperature of Fractured Plates, ° F

V-notch Charpy 15 ft-lb transition temperature

	Source plates	*Through plates*	*End plates*
Average transition temperature			
Hull plates	100.0°	63.8°	55.2°
All plates	102.5°	65.7°	56.3°
		Source-Through	*Through-End*
Difference of averages			
Hull plates		36.2°	8.6°
All plates		36.8°	9.4°
Odds against chance occurrence of these differences			
Hull plates		5×10^8 to 1	16.4 to 1
All plates		4×10^{11} to 1	35.0 to 1

The energy absorbed at failure temperatures by the source plates was consistently low. The highest value was 11.4 ft-lb and the origin of this crack was reported as being at an arc crater on a flame-cut edge.

There is a definite tendency of increased ability to absorb energy at the failure temperature when comparing source, through and end plates, see Table 7. While the differences in average energy absorption between source and end plates may be small numerically, percentagewise they are appreciable. These trends for test bars agree with those of Table 3 (per cent thickness reductions) which was compiled from service experience.

Table 7—Average Energy Absorbed at Failure Temperature Ft-Lb, Charpy V-Notch Impact Specimen

Plate thickness, in.	*Source*	*Through*	*End*
0.44–0.69	7.4	10.2	22.8
0.70–0.80	6.0	10.7	11.4
0.81–1.27	7.7	7.0	11.7
Misc. plates and shapes	5.2	6.5	7.0

The report "Investigation of Fractured Steel Plates Removed from Welded Ships,"[18] from which these data are drawn is the most important one in the field of brittle failure research to date. It gives a correlation between test specimens and service performance which allows the use of at least a semiquantitative measure in attempting to assess the reliability of a steel in service.

Confidence in this measure is heightened by the correlation between the transition temperature of the large box girders[19] and the 15 ft-lb V-notch Charpy transition temperature.

This does not mean that the temperature determined by means of the Charpy V-notch 15 ft-lb criterion is an exact or absolute measure of the probability of brittle failure in a structure. This is a very complex probability problem, the solution to which depends on other variables such as stress level, design, fabrication and service factors, as well as the material quality as measured by a test specimen. It does mean, however, that this criterion is appreciably higher percentagewise than any of the measured energy absorptions of fractured source plates and it is therefore a reasonable yardstick with which to start. Then, depending on the severity of all the other variables involved, the criterion can be upgraded or downgraded by the use of engineering judgment.

COMPARISON OF OLD AND NEW ABS STEELS

As a result of the wartime failures, ABS revised its steel specifications in order to insure more notch-tough steel, particularly in thicker plate (see Table 8). This

Table 8—New (1952) ABS Specifications

	Class A	*Class B*	*Class C**
Plate thickness, in.	1/2 or less	<1/2–1 in., inc.	Over 1 in.
Carbon maximum, %	....	0.23	0.25
Manganese, %	....	<0.60–0.90	>0.60–0.90
Phosphorus maximum, %	0.04	0.04	0.04
Sulfur maximum, %	0.05	0.05	0.05
Silicon, %	....		0.15–0.30
Tensile strength, psi			58,000–71,000
Yield point, minimum, psi			32,000
Elongation in 8 in., minimum, % (with correction for plate thickness over 3/4-in.)			21
Elongation in 2 in., minimum, %			22

* Class C plates shall be made with fine grain practice.

represents an innovation since a heretofore uncontrolled property, notch toughness, is controlled, within limits, by specifying the chemistry and steelmaking practice. Both Lloyds and the U. S. Navy have since taken similar steps.

Figure 5 is from a paper[18] to be given before the SNAME and shows a comparison of the old and the new ABS steels, based on 15 ft-lb Charpy V-notch transition temperatures. It may be seen that plates in which fractures originated came from the poorer quality wartime steels. Through and end plates were not particularly different from the average.

The new Class A plates are approximately the same as the wartime steels, as might be expected, for there have been no additional requirements for this class. This correlation between the average wartime steel and Class A plates heightens the confidence in this comparison.

The semikilled, low-carbon, high-manganese Class B plates are shown to be an improvement over Class A. The fully killed Class C plates made to fine grain practice show an even greater improvement over Class B. In this Class C group, only the metallurgical effect of increased thickness is indicated, since these are Charpy bars. In addition there will be an increase in transition temperature due to the geometrical effect of plate thickness. Tests[20] indicate that the metallurgical effect is the greater of the two.

Also shown is the large improvement due to normalizing Class C plates. Although this is not required by the specifications, it has been done for thick plates in critical areas of large tankers.

Other data[20] show the effects of normalizing on all three classes of ABS steels (see Table 9). Each pair of as-rolled and normalized specimens were taken from the same plate. There is a definite indication that normalizing improves the better grade steel the most.

There is another inference that can be drawn by combining Table 9 with Fig. 5. From Fig. 5 it can be seen that the tail of the transition temperature distribution of Class B steel overlaps the tail of the transition temperature distribution of the plates in which fractures originated. Plates in the Class B thickness range can be incorporated in critical areas of the main hull girder of fairly large ships. If the designer wishes greater assurance against the probability of brittle failure he can improve these plates in either of two ways: by normalizing or by ordering Class C, if it is available in the desired thickness. Table 9 indicates that the average transition temperature of normalized Class B plate is approximately the same as that of as-rolled Class C. However, the improvement due to normaliz-

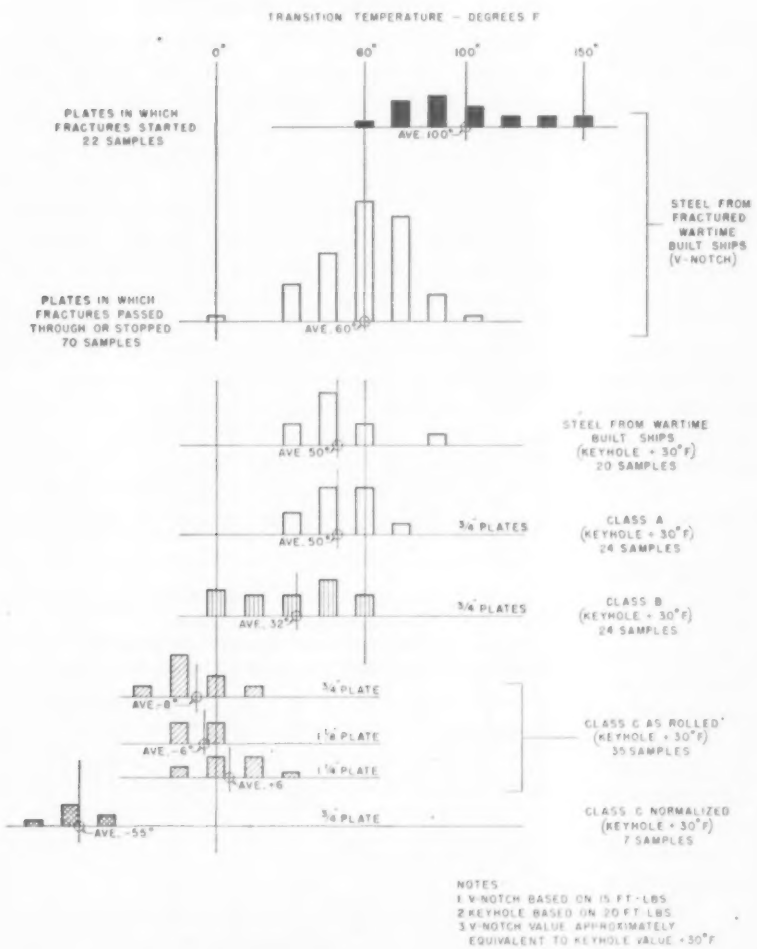
TRANSITION TEMPERATURE – DEGREES F
0°
60°
100°
150°
PLATES IN WHICH FRACTURES STARTED 22 SAMPLES
AVE. 100°
STEEL FROM FRACTURED WARTIME BUILT SHIPS (V-NOTCH)
PLATES IN WHICH FRACTURES PASSED THROUGH OR STOPPED 70 SAMPLES
AVE. 60°
STEEL FROM WARTIME BUILT SHIPS (KEYHOLE + 30°F) 20 SAMPLES
AVE. 50°
3/4" PLATES
CLASS A (KEYHOLE + 30°F) 24 SAMPLES
AVE. 50°
3/4" PLATES
CLASS B (KEYHOLE + 30°F) 24 SAMPLES
AVE. 32°
3/4" PLATE
AVE. -8°
1 1/4" PLATE
AVE. -6°
1 1/4" PLATE
AVE. +6
CLASS C AS ROLLED (KEYHOLE + 30°F) 35 SAMPLES
3/4" PLATE
AVE. -55°
CLASS C NORMALIZED (KEYHOLE + 30°F) 7 SAMPLES
NOTES:
1 V-NOTCH BASED ON 15 FT-LBS
2 KEYHOLE BASED ON 20 FT-LBS
3 V-NOTCH VALUE APPROXIMATELY EQUIVALENT TO KEYHOLE VALUE +30°F

Fig. 5 Comparison of charpy V-notch transition temperatures of wartime and present ABS steels (after Wright et al.)

Table 9—Effect of Normalizing ABS Plates

Charpy V-notch 15 ft-lb transition temperature, ° F

ABS, Class A		ABS, Class B		ABS, Class C	
As-rolled	*Norm.*	*As-rolled*	*Norm.*	*As-rolled*	*Norm.*
+32	+13	+17	−24	− 6	−60
+45	+10	+18	−18	−12	−70
+53	+28	+48	+ 8	−15	−60
+62	+46	+48	+ 5	−10	−65
+77	+45	+62	+40	− 5	−60
				+10	−50
				+15	−40
Avg. +54°	+28°	+39°	+2°	− 3°	−58°
Difference 26°		Difference 37°		Difference 55°	

ing Class B is not as certain for individual plates because it is a coarse-grained steel. In addition Class C would cost less because the cost of normalizing is about double the extra involved in going from Class B to Class C. The objection at the present time would be that this would mean ordering thinner, fully killed Class C steel than is ordinarily rolled.

It appears from the above that the only ABS Grade Steel that the engineer should consider normalizing is Class C in heavier plates, and then only for critical areas.

The foregoing comparisons are based on limited data. No doubt additional data will shift the actual figures somewhat, but it would be surprising if they altered the trends. It is apparent from the variation in each class of plates that transition temperature is quite sensitive to variations in steelmaking practice. As the causes of these variations are discovered and controlled there will be a resulting improvement in each class of steel.

FATIGUE

Although ships are subjected to alternating stresses there has been no evidence that the brittle failures were caused by typical fatigue fractures. However, it is possible that cyclic stresses may adversely affect the transition temperature without in themselves being sufficient to cause fatigue failures.

Research on this point[21] showed that the brittle transition temperature increased considerably and continuously with an increase in the number of prior stress cycles both above and slightly below the endurance limit. It was questioned whether cyclic loading at stress levels commonly used in engineering design would seriously affect transition behavior.[22] Other research work[22] showed that there was a slow growth of fatigue cracks until the nominal stress level was dropped to 40% of the notched endurance limit. At this stress level (10,000 psi) the effect on the transition temperature of 1,000,000 cycles was negligible, while at 29,700 psi (115% of the notched endurance limit) and 105,000 cycles (50% of the number of cycles required to produce failure at this stress) the effect was appreciable (see Fig. 6).

It appears from the above that the great majority of the stress cycles to which a ship is subjected have a negligible effect on the transition temperature, but that a small proportion of the total number can be high enough to have a possible contributing effect on crack initiation in way of a sharp notch. There is no evidence in the service record that cyclic stresses were a major cause of the failures, since there is no correlation between increasing service life and increasing number of failures such as would be expected if cyclic stresses were a major cause.

RESIDUAL STRESSES

Residual stresses are often referred to as welding, locked-in, reaction stresses, etc., and include those stresses not directly associated with loading or temperature differentials. At the beginning of the brittle failure investigation it was at first thought that locked-in welding stresses could have been a major factor in causing failures. Many investigations were conducted to determine their magnitude and pattern. Extensive tests were conducted independently by various building yards on the main deck and upper side shell of about sixty Liberty and Victory ships. Stresses were measured by trepanning small plugs and measuring the amount of relaxation with electric strain gages.[24]

The stress patterns were practically the same regardless of where the ships were built (whether at yards having good, fair or poor liberty ship records), the type of ship (Liberty or Victory) or of the erection or welding sequence followed. Tensile stresses of yield point

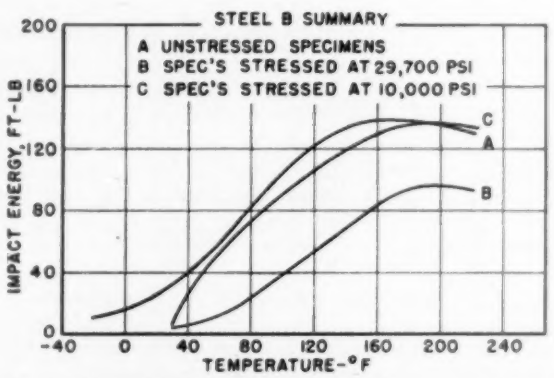

Fig. 6 Effect of fatigue on transition temperature of steel (after Lessells & Jacques)

magnitude (30,000 to 40,000 psi) were measured in butts and seams parallel to the weld, while those across the weld were low. Stresses in the deck plating away from the immediate vicinity of the welds were low and mostly compressive.

As a result of these investigations and of laboratory tests which failed to demonstrate an effect of residual stresses on fracture strength, the Board of Investigation, convened by the Secretary of the Navy, concluded that residual stresses were not a major factor in causing brittle failure.[25]

Laboratory investigations of the effect of residual stresses are extremely difficult to interpret because of the concomitant beneficial metallurgical changes which take place during stress relieving, and, if plastic flow takes place, the residual stresses are nullified.

A more recent investigation[26] showed that if the testing temperature were low enough to suppress plastic flow to the vanishing point, as-welded specimens containing defects failed at very low imposed stresses compared to similar specimens furnace or low-temperature stress relieved. Further work[27] indicated that there was no appreciable metallurgical effect due to the low-temperature stress-relieving process, or that the improvement in the specimens so stress relieved was due to the relief of stress.

The above indicates that if a critical area in a structure is operating far enough below the ductility transition so that plastic flow is virtually nil, the residual stresses and the loading stresses can be additive.

The significance of this finding in regard to ship failures is not yet clear. Fractured source plates show measurable thickness reductions of from 0.5 to 2.2% but it is not known if these are representative of the strains at the actual source. The questions to be answered are, whether strains of 0.5 to 1.0% are sufficient to nullify the effects of residual stresses and whether strains of these magnitudes are obtained at the actual fracture source at operating temperatures? If it is assumed that the answers to the foregoing are such that residual stresses are additive, a further question is raised: Is the additive effect due to the residual stresses *per se* or to the use of an unsuitable material which is operating well below its ductility transition, a condition which is not advocated by anyone?

In any event, residual stresses could only affect fracture initiation, not fracture propagation, since service experience shows that the fractures did not follow welds but ran across the plating. Reaction tensile stresses could affect propagation, if large enough, by increasing the tensile stress and therefore the stored elastic strain energy. However, the survey mentioned previously showed that plating reaction stresses in the most probable failure areas for cargo ships were not only low but mostly compressive. This reaction stress pattern is not necessarily the same for other sections of these ships, nor for all ships, as there have been plate fractures on the ways during building which can only be explained by assuming that reaction stresses furnished the driving energy. But again, were the fractures due to the reaction stresses *per se* or to an unsuitable material?

It appears that the conclusion of the Board—that "Locked-in stresses do not contribute materially to the failure of welded ships"—is still valid. This should not be interpreted as meaning that it is not possible for residual and reaction stresses to contribute to brittle failure, but rather that they were not a major cause of the extensive brittle failures in these ships.

COLD WORK

The effect of prior cold work on the transition temperature depends on the type of steel and the amount of cold work, see Fig. 7. The results are in terms of Charpy V-notch and keyhole bars. Tear tests are not suitable for measuring this effect as they introduce a variable amount of cold work during the test which obscures the effect of the prior cold work.

For small prestrains (1–2%) Fig. 7 shows that the transition temperature of rimmed and ordinary semikilled steel is affected the most, followed by low-carbon, high-manganese, semikilled and killed steels in that order. Normalizing reduces the adverse effect of prior strain even at higher strain levels for both semikilled and killed steels. It is interesting to note that some killed steels actually show a slight decrease in transition temperature for small tensile strains. This effect has been shown in other tests also.[30]

Strain aging may be minimized most effectively by the use of silicon-aluminum killed open-hearth steels.[31]

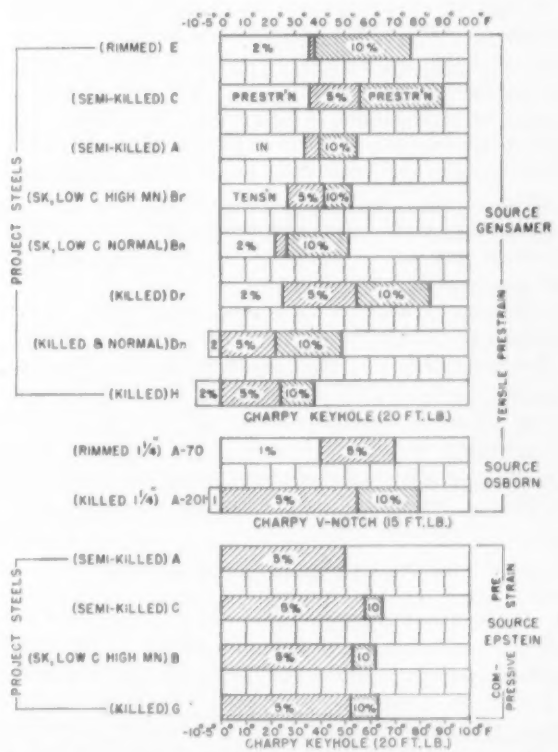

Fig. 7 Increase in transition temperature with various amounts of prestrain

In ship construction an operation which introduces cold work is rolling bilge plates and plates for tubular masts. On large ships the radius at the turn of the bilge is large (4–6 ft) and results in a strain of 1% or less. Mast plates are rolled to a smaller radius with a strain of perhaps 4%. These strains are transverse to the subsequent maximum stresses, but the effect on the transition temperature would be expected to be approximately the same as for the test specimens in Fig. 7. This indicates that 1% strain may raise the transition temperature of ABS Class B a moderate amount and Class C somewhat less, while a 4 or 5% strain could raise the transition temperature of either class appreciably.

There is another comparison which can be made from Fig. 7. The ductility transition temperatures (T_N) of Project Steels C and A were about the same when measured by Charpy bars.[28] However, the fracture transition (T_B) of Steel C, which has a higher nitrogen content, was about 60° F higher than Steel A[4,5] when measured by tear tests or wide-notched plates, both of which introduce considerable cold work into the test.

The test results for these two steels were a major reason that the notched-bar impact tests have been criticized for failing to differentiate between steels which have distinctly different fracture transition temperatures.[29]

However, Fig. 7 indicates that small strains of the order that are experienced in actual ship failures (up to 2% thickness reductions for source plates and up to 4% for through and end plates) affect the transition temperatures of these two steels by about the same amount, and that they have decidedly different transition temperatures only after strains that are larger than those experienced in service failures. Yet the impact tests which showed the two steels to have like transition temperatures were discredited for so doing. This again brings up the important unresolved question: which of these two types of test specimens is the better parameter for judging the relative ability of these two steels to resist brittle failure in a ship?

Perhaps a series of Robertson type tests, as described previously, would help to answer this question. The nominal stress levels could cover the range expected in service while the percent thickness reductions near the crack-arresting point should be similar to those experienced in service failures. The data obtained would have a bearing on fracture propagation only, not initiation, but should throw some light on the problem.

METALLURGICAL EFFECTS

In the amounts present in ship steels, the effect of carbon, phosphorus, manganese and silicon on transition temperature are greater than those of other elements. Transition temperature increases with increasing carbon and phosphorus, and decreases with increasing manganese. The amount of change depends somewhat on the type of test specimen (see Table 10).

Table 10

Element	*Change in transition temperature, ° F per 0.10% increment added*	*Test specimen*	*Source*
Carbon	+25	Charpy V-notch	Rinebolt[32]
	+30	Kahn tear test	Banta[33]
	+25	Charpy keyhole	Banta[33]
Manganese	−10	Charpy V-notch	Rinebolt[32]
	− 3	Kahn tear test	Banta[33]
	−10	Charpy keyhole	Banta[33]
Phosphorous	+130	Charpy V-notch	Rinebolt[32]
	+100	Kahn tear test	Banta[33]
	+100	Charpy keyhole	Banta[33]
Silicon	+12.5	Charpy V-notch	Rinebolt[32]
	0° to −40°	Kahn tear test	Banta[33]
	−15° to −30°	Charpy keyhole	Banta[33]

The beneficial effect of additional manganese is twofold. It not only decreases the transition temperature itself but, for the same strength level, it allows a decrease in carbon content. Phosphorous has the greatest effect for a given percentage change, but it has a much smaller allowable range.

The effect of increasing silicon varies. In Class A steels it had little effect as measured by tear tests, but was three times as beneficial as manganese when measured by keyhole specimens from the same plates.[33] In Class B plates both specimen types indicated that increasing silicon up to approximately 0.20% decreased the transition temperature at about the same rate as the Class A keyhole bars. Additional silicon then reversed the trend, although the data are scanty. An investigation of killed steels with higher silicon content[32] showed that increasing silicon increased the transition temperature. The general picture appears to be that increasing silicon, in the range used in ship steels, decreases the transition temperature at a greater rate than manganese.

Of other elements that may be present, vanadium increases the transition temperature almost as rapidly as

Table 11—Effect of Finishing Temperature on Transition Temperature

	Avg. finishing temperature	*Avg. Trans. Temp °F* —*Class A, ABS steel*— *Tear test*	*Charpy keyhole*	*Source*
	1850°	+82°	+12°	Banta[33]
	1650	+50	+ 2	
Difference	200°	32°	10°	
	1850°	106°	+29°	Epstein[30]
	1540	84	+19	
Difference	310°	22°	10°	
		Class B, ABS steel		
	1850°	+72°	−18°	Banta[33]
	1650	+32	−29	
Difference	200°	40°	11°	
	1710°	+85°	+19°	Epstein[30]
	1500	+84	+ 9	
Difference	210°	1°	10°	
		Class C, ABS steel		
	1700°	+26°	−36°	Epstein[30]
	1500	+27	−37	
Difference	200°	+ 1°	− 1°	

Table 12—Effect of 1150° F Postheat on Weld Bead Notched-Bend Specimen

Steel	Transition temperatures, ° F. — Fracture transition — As-welded	Fracture transition — Post-heated	Fracture transition — Change	2% lateral contraction — As-welded	2% lateral contraction — Post-heated	2% lateral contraction — Change
B201	50°	55°	+ 5°	55°	−100°	−155°
A212	−40	−30	+10	−50	− 90	− 40
A283	90	145	+55	20	−100	−120

(Welded at 10 ipm, 3/16-in., E6010, 175 amp., 5/8-in. plate)

carbon; copper in amounts up to 0.20% and nickel in any amount lowers it, while sulfur has little effect.

The effect of finishing temperature on the transition temperature is shown in Table 11. The tear test and Charpy keyhole specimens are paired specimens from the same heats. Each transition temperature is the average of from four to six heats. It may be seen that the amount of improvement derived by lowering the finishing temperature of the Class A and B steels depends on the type of test used as a criterion. The improvement in the inherently fine grain Class C steel is nil by either criterion over the finishing temperature range tested. Banta[33] used laboratory heats while Epstein[20] used commercial plate so that Epstein's results are probably more nearly typical of average ship steel. These data indicate that the possible improvement resulting from a lowering of the finishing temperature may not be very great in the Class B and C steels where it is desired.

WELDMENTS

The behavior of as-rolled plates gives no indication of the characteristics of welded plates.[1] The strength of a welded joint cannot be predicted by the performance of the plate forming the joint and the weld metal but is a complex function of both.[34]

Lehigh and Kinzel weldbead notched bend specimens, weldbead notched tension specimens and welded tee-bend specimens all rated Project steels in the same order of performance.[35] This order agreed with that obtained by other investigators using other specimens. Project steel "Br" had a lower transition temperature than "C" steel, while "A" and "W" steels were in between.

Stress relieving at 1150° F lowered the ductility transition of weld bead notched-bend specimens appreciably, but raised the fracture transition[1] (see Table 12). Similar tests by the same investigators[11] on a rimmed, an ordinary semikilled steel and a low-carbon, high-manganese steel showed the same results. Stress relieving at 1200° F lowered the ductility transition 50 to 100° F while the fracture transition was raised 30 to 40° F. Using eccentric notched bars, other investigators[36] found that the zone of low ductility, 0.3 to 0.4 in. from the weld centerline, was practically eliminated by an 1100° F postheat.

Preheating to 400° F improved the ductility of the critical zone. Preheating weld bead specimens from 150 to 400° F lowered the transition temperatures. No important gain was obtained by using 1150° F postheat that was not obtained by 400° F preheat. Other test results on large (30 x 36 x 3/4 in.) semikilled specimens with multipass E6010 welds, similar to those made in production, show the same result.[37] Preheat of 400° F was as effective as 1200° F postheat.

It has been pointed out[38] that as the width of the heat-affected zone increases the transition temperature is raised. In general, as the travel speed is increased, the width of the heat-affected zone is decreased for a given energy input per linear inch of weld.

Other notched weld-bead tension tests[20] showed that 10% prior tensile strain did not adversely affect the transition temperature of ABS Class B and C steels, while normalizing before welding produced a decided lowering of the transition temperature of welded Class C specimens.

Explosion tests[39] of ABS Class B and C plates both unwelded and welded with various electrodes are shown in Fig. 8. It may be seen that welding always lowered the performance below that of the prime plate. Submerged arc and low-hydrogen electrode welds resulted in much better performance than E6010 manual welds, and low-hydrogen welds improved the Class C weldments much more than the Class B, particularly at lower temperatures. Making the root passes of the submerged arc joint with E6010 greatly reduced its performance.

Other explosion tests[40] showed that scattered porosity did not significantly affect the performance of the weldments.

From the above findings it appears that the use of better steels and low-hydrogen welds (which includes submerged arc welds) result in better weldments although the amount of improvement cannot be predicted from the individual properties of the plate and weld metal. The higher amperages necessary to achieve a fast travel speed and narrow heat-affected zones are also desirable from a production standpoint. Of particular interest are the results which show that 400° F preheat is as effective as stress relief in lowering the ductility transition and presumably without the adverse effect of the latter on the fracture transition. Stress relief in general, is impracticable for hull weldments, but preheat can be accomplished in whole or in part by welding technique alone, such as reverse cascade.

Although tests show that welds are inferior to prime plate material, the service record shows almost no evidence of failures starting in sound welds. Defective

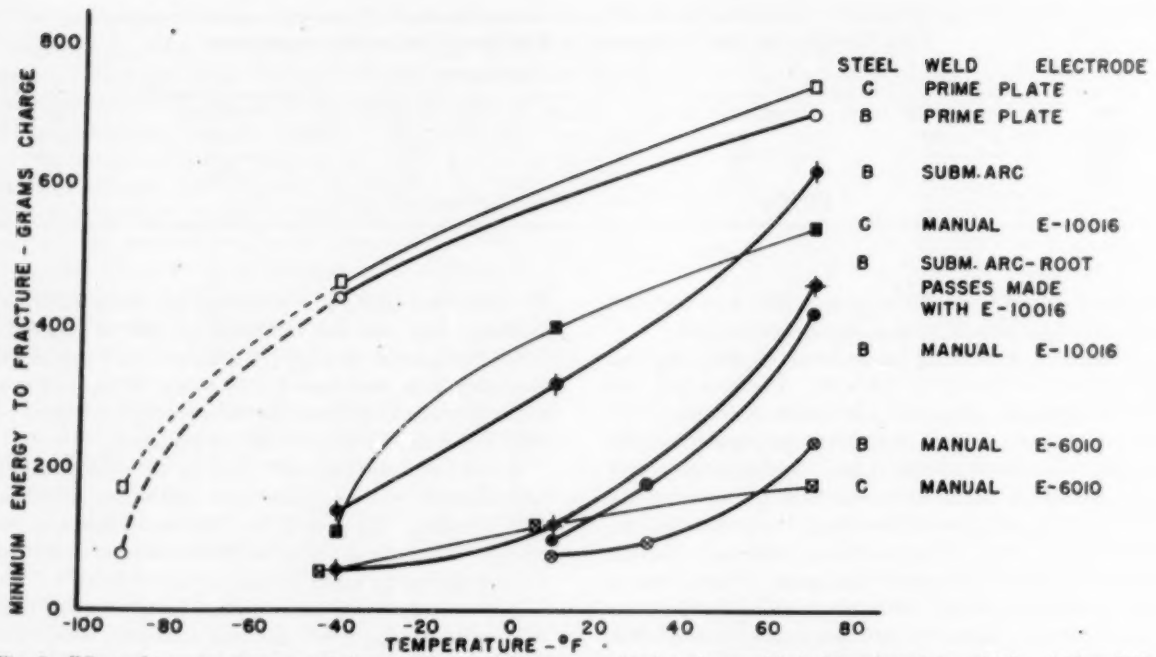

Fig. 8 Effect of temperature on minimum energy to fracture welded and unwelded ship plate 1 in. thick. (After Mikhalapov)

welds, on the other hand, were the greatest single cause of failure. A survey of recent Class 1 and 2 structural failures on war-built ships showed that approximately 40% definitely originated in defective welds.[41]

Several building yards are now using X-ray inspection as a means of quality control for hull welds. It is possible that this valuable tool is not always utilized to achieve maximum effectiveness.

With a system of random X-ray inspection that traces defects back to individual welders and energetic supervision that is competent to give additional training in welding to meet X-ray standards, practically any standard of quality that is desired can be achieved, provided that it is demanded. Hull welding to meet X-ray standards is now going through the phase that P-1 pipe welding went through previously when X-ray standards were introduced for that class of work. It was found that many years of experience on non-X-rayed welding was no guarantee that the welder could consistently produce welds that met X-ray standards. However, after additional training by competent supervision the majority of the welders had an extremely low repair rate, most of which was for defects of a minor nature. Those welders who could not learn were transferred to less critical work.

Some defects are caused by poor fit-up, and X-ray inspection will allow correction of these faults also. In locations where X-rays cannot be taken, magnetic particle inspection and occasional flame gouging will result in improved weld quality.

The vigor with which these inspection tools are used is important for, as mentioned previously, the service record shows that defective welds were the greatest single cause of failure.

DESIGN

The tremendous improvement in service performance of the better designed, Victory-ship-type hatch corner over the Liberty type has already been mentioned. This service record is important for it shows a correlation between better service performance and increased energy absorption in test specimens.

Tests of flat plates with flame-cut edge notches[42] showed that the energy absorbed to maximum load and to fracture is severely impaired by such edge notching for both ductile and cleavage failure. A flame-cut slot was as severe as a jeweler's hacksaw cut. The average energy absorbed to maximum load by cleavage failure specimens with flame-cut notches of 0-, 90- and 135-deg included angles were only 5% to 7½% of the amount absorbed by unnotched plates, while semicircular, 1½-in. radius notches absorbed 33%. When the notch flanks were separated by a 15-in. long reduced width section the average energy absorptions of the 0- and 90-deg included angle notched plates were 14 and 21%, while the semicircular type absorbed 48% of the energy that the unnotched plates did. These tests show that root radius is more important than flank angle in determining the amount of energy absorbed.

Other tests[43] showed that diagonal slots and notches exhibited greatly increased values of energy absorption and greatly decreased transition temperatures compared to slots and notches perpendicular to the face of the plate.

Another series of tests[44] were run to determine the effectiveness of three types of arc-welded reinforcement, face bars, single doubler plates and insert plates for

circular, square with rounded corners and square with sharp corner openings in steel plate. The ultimate strength of plates with openings varied from approximately 75 to 100% of that of the plain plates and decreased in a linear manner with the logarithm of the ratio of the half width of the opening to the corner radius.

The energy absorbed to maximum load of the plates with openings was only from 6 to 34% of the same values for the plain plates. The logarithm of the energy absorption decreased in a linear manner with the logarithm of the ratio of the half width of the opening to the corner radius. Increasing the percentage of reinforcement increased the ultimate strength of the plates but made no sizable change in the energy absorption to maximum load. There was much better correlation between energy absorption and shape of opening than with the type of reinforcement.

All of these fractures were ductile but the trends shown are valuable. Further work is planned which will include tests at temperatures low enough to produce cleavage failures.

Tests have also been conducted[45] on large specimens representing the intersection of a bottom longitudinal and a transverse bulkhead on tankers. These specimens were full scale except for the $^1/_2$-in. bottom plating instead of $^3/_4$-in. plate. They showed that high stress concentration existed with some details but not with others and that stresses ranging from 1.5 to 2.2 times the average existed at the center of the bottom plate and the lower edge of the bracket. Additional tests have been made on a second series of specimens with full thickness bottom plating, but have not as yet been published.

Full-scale corrugated bulkhead intersections have been tested.[46] Stresses at the intersection of the tee web and the sloping sides of the longitudinal bulkhead were 2.1 to 3.4 times the average in the longitudinal bulkhead. The specimen which was modified by the addition of flanged angles at the bulkhead connection gave the best performance whether based on maximum load, energy to failure or reduction of stress concentrations.

These full-scale tests should be of sufficient interest to engineers and designers to be read in full, including the discussions.

Tests of structural components should be encouraged in order to discover better methods of material distribution. As shown by the tests already conducted, changes in detail design can result in increasing the maximum load achieved and, in particular, in large increases in the energy absorbed.

Such tests should be run not only for their own sake but because if there is a national emergency, requiring a large shipbuilding program, the present better grade ship steels may not be available in sufficient quantities. Design and fabrication will then have to furnish the plus factor for the margin of safety instead of, as now, depending on a superior grade of steel plate.

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The Preheating and Postheating of Pressure Vessel Steels

PVRC-sponsored report summarizes present knowledge of preheating and postheating with special reference to pressure vessel steels and points out gaps in knowledge which require further investigation

by Robert D. Stout

Foreword

This report was prepared for the Materials Division of the Pressure Vessel Research Committee. It is a summary of present knowledge concerning the effects of preheating and postheating operations on the soundness and mechanical properties of welds.

An important part of the report is a table containing recommended preheating and postheating temperatures for various carbon contents and thicknesses of the grades of steel commonly used for pressure vessels. A list of problems requiring further study is also included.

The author is grateful for the invaluable guidance in preparing the report provided by T. N. Armstrong, H. C. Boardman, R. E. Somers and members of the Materials Division.

I. INTRODUCTION

THE techniques of preheating and postheating make it possible to weld successfully structures and steel compositions which otherwise could not be joined by certain welding processes. Both these techniques have long been used in other branches of metal technology: preheating in the heat treatment of high alloy or intricately shaped steel parts, postheating in the tempering of hardened steel and in the stress relieving of castings and forgings. In some respects the purposes of these treatments are the same for weldments as for tools, castings and forgings, but in others they are special to welding.

This report has been prepared to summarize the present knowledge of preheating and postheating with special reference to pressure vessel steels and to point out gaps in the knowledge which require further investigation. In the next two sections the important effects of preheating and postheating are reviewed. In subsequent sections a more detailed discussion is given of the physical and metallurgical effects of each, as shown by recent investigations on the subject. An important part of the report is a series of tables in which specific recommendations are given for the preheating and postheating of various thicknesses and grades of pressure vessel steels.

Robert D. Stout is Professor of Metallurgy at Lehigh University, Bethlehem, Pa.

II. WHAT PREHEATING DOES

Preheating consists of raising the temperature of the parts to be welded to some level above the ambient temperature. It may be applied over the whole part or locally at the site of welding. On a practical basis, the welding engineer uses preheating because it helps:

1. To prevent the formation of cracks in the weld metal or in the heated base metal.
2. To reduce distortion of the weldment.
3. To avoid loss of ductility and toughness in the weld and base metal.

These practical benefits are obtained from preheating by the following actions: (*a*) The thermal gradient is reduced. Correspondingly the forces set up by the thermal contraction of the weld are lower and the danger of cracking and the tendency for distortion are reduced. (*b*) The cooling rate is reduced. The steel tends to form soft rather than hard microstructures; dissolved hydrogen is given a chance to escape without harm; transformation stresses are mild. Thereby base metal cracks are avoided and the region of the weld remains tough and ductile.

The effects of preheating are described in detail in Section V.

The most significant effect of preheating is that it reduces the temperature gradient between the weld and the surrounding base metal. If the thermal gradient is lowered, the thermal strains set up between the cooling weld and the adjacent parent metal are reduced. At the same time the rate of cooling of the weld zone is slower. Both of these effects favor the production of a satisfactory welded joint.

Weld metal cracking and distortion originate from the

resistance which the members being welded offer to the natural thermal contraction of the weld as it cools to ambient temperature. If the constraint is high, the weld is forced to undergo plastic flow in adjusting to the confines of the weld joint and cracking may occur. If the constraint is low, the weld may exert sufficient pull to warp the structure. By a better distribution of contraction of the weld and the joint members, preheating reduces both the tendency to crack and to distort.

The cooling rate imposed on the weld following solidification is determined to an important extent by the temperature and volume of the base metal adjacent to the weld. High cooling rates can be equivalent to a quench and may result in base metal cracking or in the formation of low-ductility regions in the heat-affected zone of higher carbon or alloy steels. Preheating alleviates the danger of these effects by slowing the cooling rate impressed on the weld.

In general, the degree of preheating required increases with the section thickness and with the carbon and alloy content of the steel. While the necessity for preheating depends on the nature of the weldment involved, it is permissible to make general recommendations for preheating on the basis of the thickness and composition of the steel to be welded. Such recommendations are limited to the purpose of avoiding cracking in the weld metal or base metal during assembly. Therefore in Table 1 are presented recommended preheating temperatures for the commonly used grades of pressure vessel steels. These recommendations are deliberately conservative. For example, low-carbon steels often are not preheated even in sections up to 4 in. in thickness. The preheat temperature of 100° F is used to indicate warming of the weldment when shop or field temperatures go below 50° F in wintertime. The use of EXX15-16 electrodes is designated to prevent underbead base metal cracking. Use of these electrodes does not eliminate the need for preheating to reduce distortion or to avoid loss of ductility in the heat-affected zone of the base metal.

III. WHAT POSTHEATING DOES

Postheating consists of heating the weldment to a sufficiently high temperature to permit certain desired changes to take place in the steel. Like preheating, it may be applied to the welded joint locally or to the whole structure. Postheating is used to produce two principal effects:

1. Residual stresses can be relieved to a low level.
2. The heat-affected base metal is softened and toughened by the metallurgical changes, and any dissolved hydrogen present is given an added opportunity to escape.

The most common type of postheating is that carried out in the temperature range of 1100–1300° F., often termed "stress-relieving" or "stress-relief annealing." Carbon steel at temperature of 1100–1300° F has a yield strength of 2000 to 10,000 psi and thus the residual stresses relax to the same level while at temperature. If the heating is not too localized and if cooling is

Table 1—Recommendations for Preheating and Postheating Pressure Vessel Steels

Steel grade	*Carbon range, %*	*Thickness range, in.*	*Minimum preheat and interpass temperature, ° F*	*Postheat temperature, ° F*
1. All carbon steels such as: A30 A201 A212 A283 A285 ABS—A, B and C grades	Under 0.25	Up to 1	None	None
		1–2	100 or E6015-16	1100–1250 desirable
		2–4	200	
	0.25–0.30	Up to ½	None	None
		½–1	100 or E6015-16	
		1–2	200 or E6015-16	1100–1250 desirable
		2–4	300 or 100 with E6015-16	
	0.31–0.35	Up to ½	100 or E6015-16	
		½–1	200 or E6015-16	
		1–2	300 or 100 with E6015-16	1100–1250 required
		2–4	300	
2. High-tensile alloy steels such as: A202 A225 A299 A302 A204	Under 0.15	Up to ½	None	None
		½–1	100 or EXX15-16	1100–1250 desirable
		1–2	200 or EXX15-16	
		2–4	300 or 100 with EXX15-16	1100–1250 required
	0.16–0.20	Up to ½	100 or EXX15-16	
		½–1	200 or EXX15-16	1100–1250 desirable
		1–2	300 or 100 with EXX15-16	
		2–4	300 or 200 with EXX15-16	1100–1250 required
	0.21–0.25	Up to ½	200 or EXX15-16	1100–1250 desirable
		½–1	300 or 100 with EXX15-16	1100–1250 required
		1–2	300	
	0.26–0.31	Up to ½	300 or 100 with EXX15-16	
		½–1	300 or 200 with EXX15-16	1100–1250 required
		1–2	300	
3. Nickel steel: A203	Under 0.15	Up to 1	300 or 100 with EXX15-16	
		1–4	300 or 200 with EXX15-16	
	0.16–0.21	Up to ½	300 or 100 with EXX15-16	
		½–1	300 or 200 with EXX 15-16	1100–1150 postheat required
		1–4	300	
	0.22–0.27	Up to ½	300 or 200 with EXX15-16	
		½–1	300	
		1–4	400	

slow and even over the section, the stresses will remain low, down to room temperature. The yield strength of the carbon steel is largely recovered. Stress relieving frequently is applied to obtain dimensional stability of the weldment during subsequent machining operations.

While there is some thought that relief of residual stresses, particularly when applied to heavy sections, improves service performance and that stress-relief is therefore the principal purpose for postheating, a number of investigators have indicated that postheating improves the service properties primarily by means of metallurgical changes. This point will be amplified in Section VI.

Postheating, like preheating, is more likely to be required as the thickness of the section and carbon plus alloy content increase. Table 1 also gives recommendations for postheating the grades of steel commonly used in pressure vessels.

IV. PREHEATING VS. POSTHEATING

Frequently the question has been raised when postheating is specified by codes or other regulations as to whether the proper use of preheating in specific cases eliminates the need for postheating. There is at least laboratory evidence[1,*] that in some cases there will be no loss in ductility in the weldment if postheating is eliminated, providing the proper preheating procedure has been employed.

Preheating and postheating are equivalent, to some extent, in their influence on the portion of the base metal affected by the heat of welding. Both tend to promote a softer, tougher weld-heated zone: preheating by the slower cooling rate which permits softer microconstituents to form in the heated zone, postheating by tempering any hard microconstituents formed. On the other hand, the two techniques each have their limitations.

Preheating, unless carried out at an unusually high temperature, generally does not reduce the peak residual stresses in the weldment to a low level. Thus dimensional stability, stress-corrosion resistance and (perhaps) fatigue strength, as far as affected by residual stresses, are not greatly improved. Highly hardenable alloy steels, such as the 5% Cr, Mo steels, cannot be kept from hardening in the heated zone unless relatively high preheating temperatures are used.

Postheating, unless applied before the weld cools down, is too late to be of any help in preventing cracking in the weld or heat-affected zones. It generally is of little benefit to the ductility or toughness of the weld metal, lowering its strength and, in the case of some high-tensile alloy deposits, may be damaging by causing temper embrittlement.

The relation of preheating to postheating, therefore, depends on the purposes for which they are applied. Some structures may require only preheating, some only postheating and others may require both.

V. THE PHYSICAL AND METALLURGICAL EFFECTS OF PREHEATING

Methods

Preheating can be accomplished by any convenient heating source. While the most effective method to obtain uniformity might be to place the structure in a furnace, in many shops and in the field less elaborate methods must be used, especially for large structures or low preheat temperatures. Sources of heat such as gas burners, coke fires, infrared lamps and low-frequency induction coils are used. The heat may be applied locally or over the entire structure. The temperature is controlled through use of contact pyrometers or temperature-sensitive crayons.

Physical Effects

Preheating reduces the thermal gradient between the weld and the surrounding base metal. The lower thermal gradient retards the cooling of the weld and also lessens the thermal strains set up in the structure.

There are two ranges of temperature in which the magnitude of the cooling rate is important. The first is in the general range of steel phase changes, from 1300 to 600° F, where austenite transforms to other microconstituents. Austenite can transform to martensite below 600° F, but this brittle constituent is undesirable in weldments. The second is between 400 ° F and room temperature where microcracks may form in the weld metal and underbead cracking may occur in the base metal.

Extensive investigations[2, 3] have been carried out to establish the effects of welding conditions, metal mass and preheat temperature on the cooling rate. It has been shown, for example, that the cooling rates at 1300° F are lowered about 15% by a 200° F preheat and about 35% by a 400° F preheat with the heat inputs associated with manual welding. In the range below 400° F the reduction in cooling rate is much more marked, especially for large sections. In Figs. 1 and 2 some typical cooling rates are given for various preheat temperatures.

When two steel members are joined by welding, the heat of welding sets up a heating and cooling cycle in the vicinity of the weld. During the heating phase the base metal is locally expanded. When the weld solidifies and cooling commences the weld metal and heated zone undergo contraction. If unimpeded they would contract equally in all dimensions. In an actual joint, the adjacent metal is held or supported by its own stiffness, by previous welds or by jigging arrangements. When the weld attempts to pull in the joint edges and become shorter, it is opposed by the rest of the metal and is therefore put under tension. While the weld is still hot, it is relatively weak and plastic and thus will adjust it-

* See also References 28, 36, 40 in the Bibliography of References.

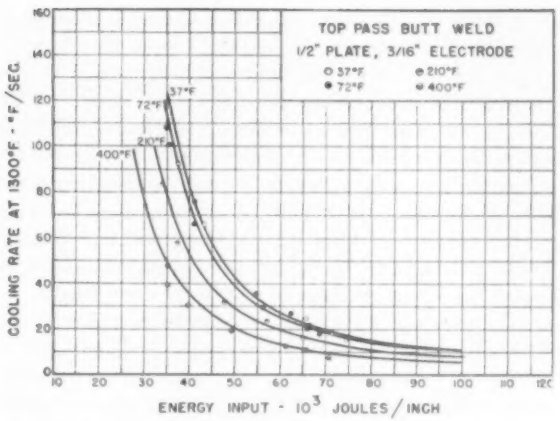

Fig. 1 Effect of preheat temperature on cooling rates in welds (Hess, et al.[3])

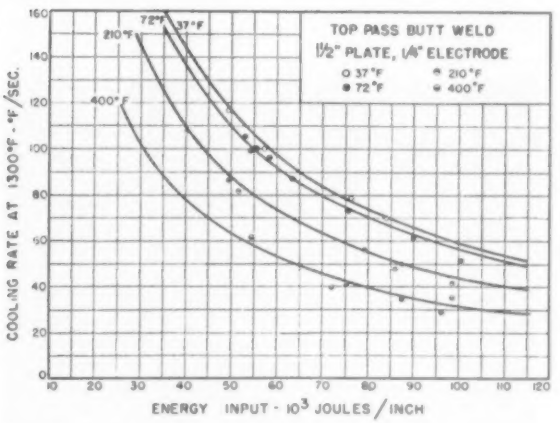

Fig. 2 Effect of preheat temperature on cooling rates in welds (Hess, et al.[3])

self by plastic flow. As it becomes colder, it becomes stronger and sets up higher stresses before it undergoes further plastic deformation. In a structure of low rigidity these forces will cause warping, while with high rigidity a weld metal of limited plasticity may crack open, particularly in the root pass of butt welds.

Because it is only in the range of temperatures below a dull red heat, say 1200° F that the weld can set up appreciable contraction forces, preheating has a more potent influence on reducing root-cracking or warping than might be expected. Measurements[4] have been made of the transverse movement in the weld zone for various preheating temperatures and are reproduced in Fig. 3. In cooling from 1200 to 400° F contraction is about two-thirds of that encountered in cooling from 1200° F to room temperature. In the same investigation[4] root cracking of butt welds was found to occur below 400° F, so that the plastic flow of the weld below that temperature is especially critical.

While the peak stresses in the weld zone approach the yield strength of the metal whether preheating in the usual ranges is used or not, the reaction (general)

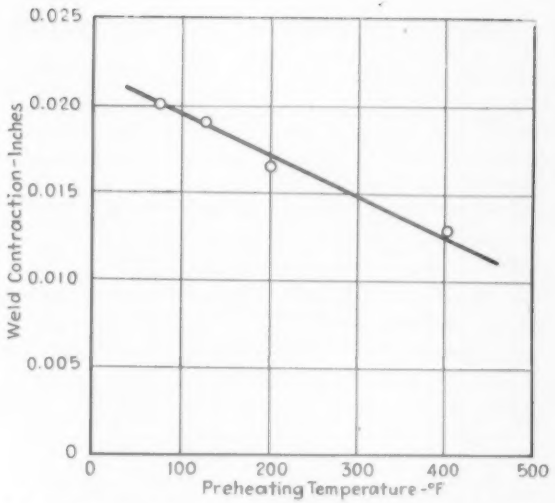

Fig. 3 Effect of preheating on the transverse contraction of the weld (measurements taken at a 1-in. gage length placed across the weld groove)

stresses in the structure usually are lower with over-all preheating. Because the weld contraction is reduced by preheating as shown above, the energy stored in the structure, which is largely in the form of reaction stresses, is therefore also lower. In this connection the influence of localized preheating compared to over-all heating is of concern. Unless the preheating is applied in a way which avoids upsetting of the metal and constraint by surrounding cold metal, the preheating may actually raise the reaction stresses rather than lower them. An excellent example is the simple case of a spoke in a wheel which is cracked and is to be repaired by welding. If only the cracked spoke is preheated and welded, the contraction of the weld will be supplemented by the contraction of the spoke itself in cooling and because of the resistance of the rim, the residual stresses will be higher than without preheating. In such a case it would be better to preheat adjacent spokes while keeping the spoke to be welded cold. Unless properly applied, localized preheating may be of no service in reducing reaction stresses and warping.

Metallurgical Effects

The metallurgical effects of preheating are associated with the slower cooling rates it provides. When steel heated to a high temperature is cooled it transforms to microconstituents whose characteristics depend on the rate of cooling imposed on the steel. In Fig. 4 the strength and ductility of a 0.25% carbon steel are shown as a function of the cooling rate. The higher the cooling rate, the harder and less ductile is the steel. Thus the weld-heat-affected zone will vary in ductility and toughness with the weld cooling rate. By lowering the cooling rate, preheating raises the ductility and toughness of the weld-heat-affected zone.

An illustration of the effect of preheating on the toughness of welded steel plate of 0.25% carbon is given

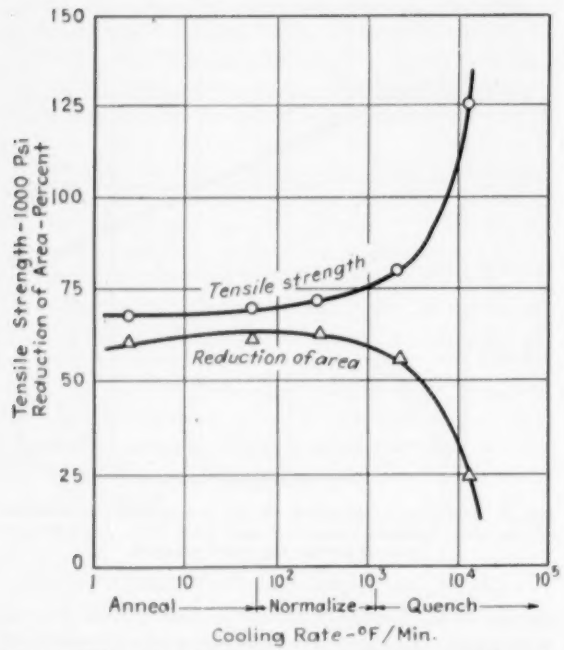

Fig. 4 Effect of cooling rate on the tensile properties of a 0.25% carbon steel

in Fig. 5. These tests, using the longitudinal bead notch-bend test, show that the transition temperature of the welded steel is lowered considerably by preheating.

As the carbon and alloy contents increase, the cooling rate must be decreased to avoid brittle heat-affected zone, and so higher preheat is necessary. Some alloy steels require preheating up to 600° F or higher followed either by an hour hold at temperature after welding or alternately, by allowing the weld to cool to 250° F and then postheating at 1350° F for half an hour.

Bland[5] and Flanigan[6] have shown that weld metal suffers a loss of ductility if it is cooled rapidly from 400° F to room temperature. This loss of ductility they ascribe to the formation of minute cracks in the weld metal by the action of dissolved hydrogen. If the hydrogen fails to escape during cooling it eventually collects at small discontinuities in the metal, building up sufficient pressure to cause small tears or microcracks. By reducing the cooling rate, preheating gives the hydrogen an opportunity to diffuse out of the steel at moderately elevated temperatures without damage to the weld metal. The range of welding conditions for which this effect is important is not yet established.[7]

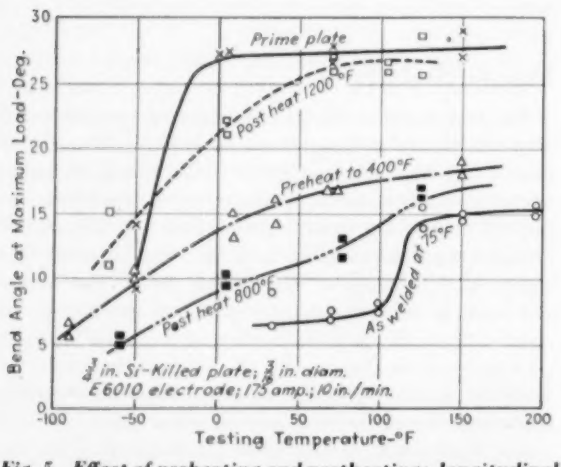
Bend Angle at Maximum Load-Deg.
Prime plate
Post heat 1200°F
Preheat to 400°F
Post heat 800°F
As welded at 75°F
¾ in. Si-Killed plate; ⅜ in. diam. E6010 electrode; 175 amp.; 10 in./min.
Testing Temperature-°F

Fig. 5 Effect of preheating and postheating: longitudinal notch-bend test[8]

Hydrogen can also cause underbead cracking in the heat-affected zone of the base metal as illustrated in Fig. 6. This cracking, too, will occur only at cooling rates sufficiently high to harden the heat-affected zone. As previously stated, the rate of cooling at which high hardness occurs is a function of carbon and alloy content. By preheating, hardening will be decreased or avoided, and the hydrogen will escape without causing cracking.

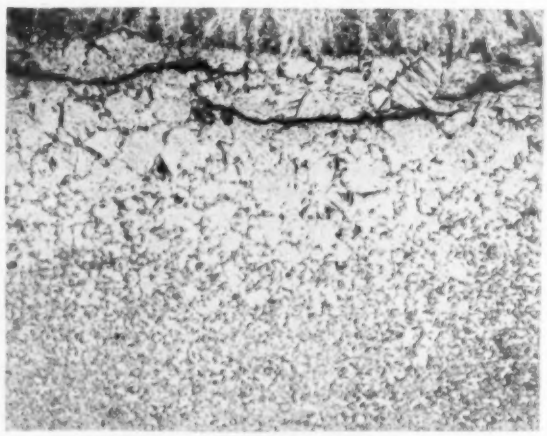

Fig. 6 Underbead cracking 500 × nital etch (Voldrich)

The question may be asked whether there are any undesirable effects of preheating. One possible undesirable effect is the broadening of the heated zone and the increased grain size in the area next to the fusion line. Another is strain aging in sensitive steels which have previously been cold formed. Both of these effects tend to reduce the toughness of the weldment. Laboratory tests would indicate that in most cases they are not important.

As far as metallurgical effects are concerned, local preheating is just as effective as over-all preheating so long as it is produced in a sufficiently wide band along the weld to avoid chilling effects from the unheated metal.

VI. PHYSICAL AND METALLURGICAL EFFECTS OF POSTHEATING METHODS

Methods

Postheating may be performed in a furnace or by the

application of controlled amounts of heat from gas burners, electrical heaters and the like. Temperatures usually will range up to 1300° F or higher, and the time temperature may be from a few minutes to several hours.

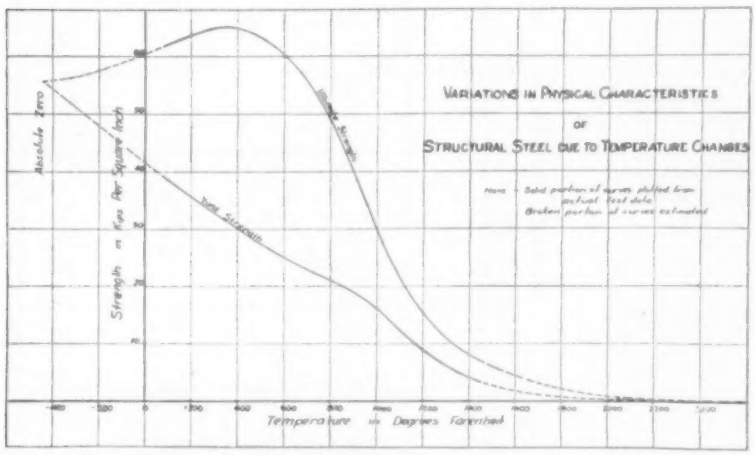

Fig. 7 Variations in physical characteristics of structural steel due to temperature changes (Spaulding[12])

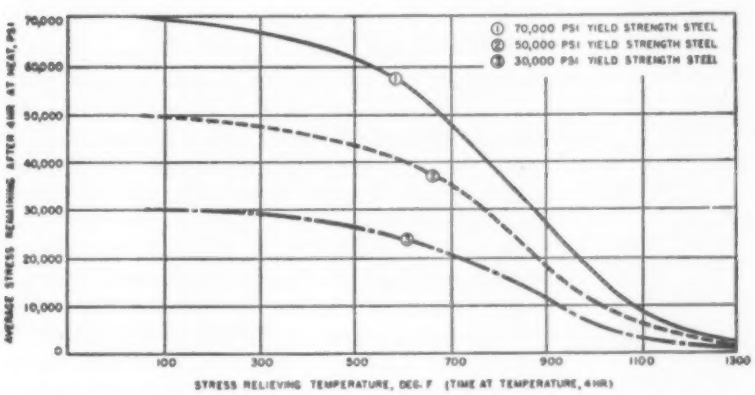
① 70,000 PSI YIELD STRENGTH STEEL
② 50,000 PSI YIELD STRENGTH STEEL
③ 30,000 PSI YIELD STRENGTH STEEL
AVERAGE STRESS REMAINING AFTER 4HR AT HEAT, PSI
STRESS RELIEVING TEMPERATURE, DEG. F (TIME AT TEMPERATURE, 4 HR)

Fig. 8 Stress-relief temperature vs. average stress remaining (AWS Welding Handbook, Third Edition)

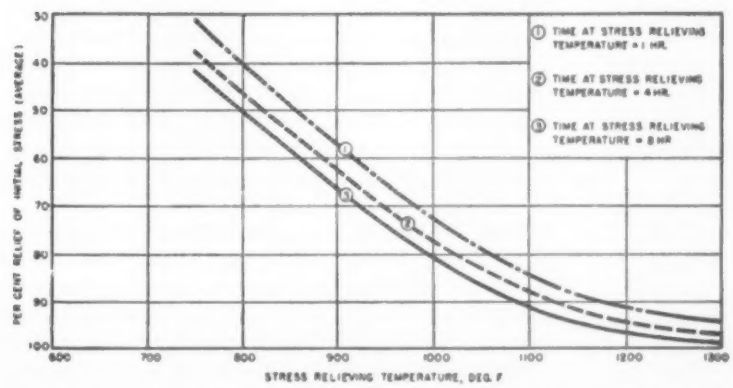
① TIME AT STRESS RELIEVING TEMPERATURE = 1 HR.
② TIME AT STRESS RELIEVING TEMPERATURE = 4 HR.
③ TIME AT STRESS RELIEVING TEMPERATURE = 8 HR.
PER CENT RELIEF OF INITIAL STRESS (AVERAGE)
STRESS RELIEVING TEMPERATURE, DEG. F

Fig. 9 Stress-relief temperature vs. per cent total stress remaining (AWS Welding Handbook, Third Edition)

Physical Effects

The most conspicuous physical effect of postheating is the relief of residual stresses. As steel is heated, its yield strength, as determined in a short-time tension test, drops progressively as shown in Fig. 7. Since the residual stresses at a given temperature cannot exceed the yield strength at that temperature, they are lowered by plastic yielding. This is illustrated in Fig. 8. Certain alloying elements such as Mo, W, Cr and V raise the high temperature strength of steel, and steels containing these alloys require higher postheating temperatures for effective stress relief.

If the time at temperature during postheating is extended, the residual stresses will be further relieved by the action of creep, but this effect requires such long times that it is usually more economical to use a postheat high enough to accomplish stress relief in 1 or 2 hr rather than to use a lower temperature for much longer times. See, for example, Fig. 9. It is generally satisfactory to use a temperature high enough to lower the peaks of residual stress to a few thousand pounds per square inch. For most pressure vessel steels the range from 1100 to 1300° F is suitable.

There are several precautions that must be observed if stress relief is to be the final thermal treatment for the structure. The heating must be carried out over a sufficient portion of the structure so that the expanded metal does not upset by reaction with surrounding cold metal and re-establish the stresses upon cooling. This can easily happen when local postheating is misapplied. Second, the cooling must be slow and uniform to avoid excessive thermal gradients in the structure, which would reimpose residual stresses.

The influence of residual stresses on the service properties of a weldment is still a subject of controversy. It is well known that these stresses will cause difficulty in machining to exact dimensions and thr they promote stress corrosion. There is conflicting evidence[8,9] on the effect of residual stresses on notch toughness. Figures

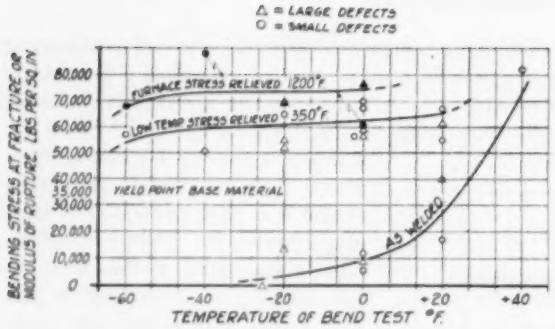

Fig. 10 Load carrying capacity in bending, at various testing temperatures, welded plates with jeweler's saw-cut defect (T. W. Greene[9])

10 and 11 demonstrate the conflict. The tests of Greene in Fig. 10 were conducted on a butt-welded plate, 30 by 36 in., in which a jeweler's saw-cut served as notch. The criterion of toughness used, i.e., modulus of rupture, is somewhat uncommon. Failure was determined by the appearance of a crack during bending. The tests at Lehigh shown in Fig. 11 were made on two series of plates, 6 by 12 in., one with a high constraint in the weld members, the other with as little constraint as possible. The residual stresses in the high constraint specimens were 19,000 psi longitudinal, 31,000 psi transverse. In the minimum restraint specimens, they were 6000 psi longitudinal, 15,000 psi transverse. No difference in behavior was observed for the two restraint levels.

If the steel in a welded structure has even a small amount of ductility, the first application of load which sets up stresses exceeding the yield strength will cause plastic flow and relieve the residual stress peaks. Only a few tenths of 1% strain are required to accomplish relief. Therefore, residual stresses can cause premature failure only in a region of the structure that is totally brittle; even the brittle fractures of ships and pressure vessels that failed always have shown some deformation at the fractured edge—usually 1% or more reduction in thickness. Such deformation would be more than enough to wipe out residual stresses.

However, some authorities regard residual stresses as seriously detrimental to service performance. This appears to be especially true in the case of heavy-walled pressure vessels. Such laboratory investigations as those presently available are not adequate to prove or disprove the point. Service reports are equally inconclusive.

Stresses can be relieved by mechanical means as well as thermal. Hydrostatic stressing and low-temperature stress relief by gas flames are examples. Kennedy,[10] who was one of the original advocates of low-temperature stress relief, stated that the metallurgical effects of postheating contribute more in restoring notch toughness than do the stress-relieving effects. However, the relative merits of low temperature stress relief annealing are still controversial.

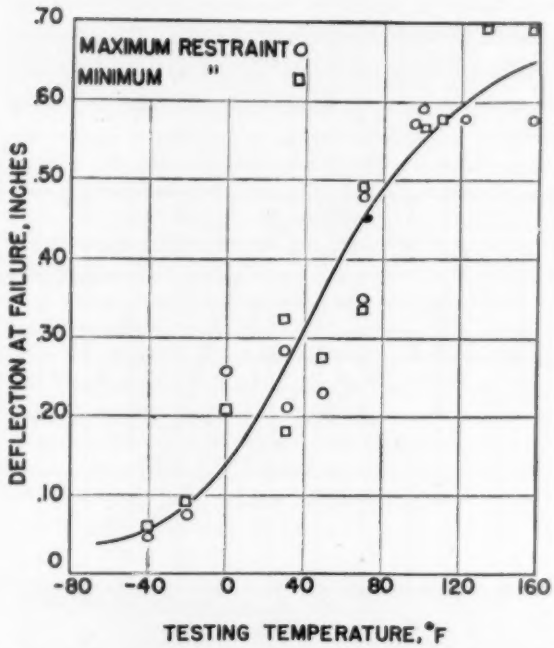

Fig. 11 Comparison of specimens welded with low and high levels of constraint to weld shrinkage (see text for residual stress values measured in each)

Metallurgical Effects

Metallurgically, the effect of postheating is to soften the hardened or partially hardened microstructures formed in the weld zone and to allow the escape of hydrogen if it is present. The tempering action of postheating restores ductility and toughness to the heat-affected zones of higher carbon or alloy steels.

The action of postheating on the notch toughness of welded steel is progressive as the temperature is raised. In Fig. 5 the notch toughness of a welded 0.25% carbon steel is shown as a function of postheating temperature. Certain alloy steels such as the Cr-Mo types require slightly higher temperatures for softening and toughening. If the temperature exceeds the transformation range of the steel, the effects of a full treatment—annealing if slow cooled or normalizing if air cooled—are obtained. The toughness of the welded steel may then approach or exceed that of the unwelded material.

The necessity for postheating generally increases with carbon content, alloy content and thickness of section. The need for postheating can often be avoided by the proper choice of welding electrode, welding current, travel speed and by preheating to maintain the desired toughness in the weld zone.

There are limitations to postheating which must be noted. It obviously cannot heal weld or base-metal cracks if they originate during welding. Broadly speaking, postheating does not improve the properties of the weld metal. It reduces somewhat the high yield strength characteristics of the deposit without a compensating gain in toughness. Moreover, some of the high-tensile weld metals are definitely embrittled by

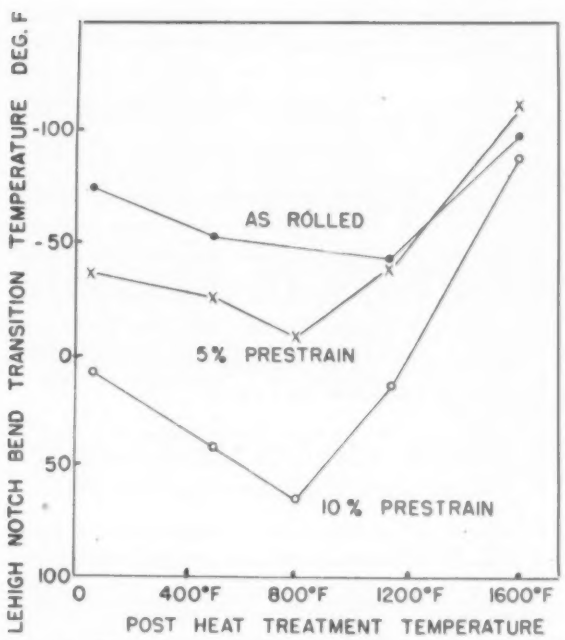

Fig. 12 Effect of prestrain and heat treatment on notch toughness of 1¼-in. A-201 steel

postheating in the usual 1000–1200° F range.[13] This phenomenon is called temper embrittlement and is probably caused by precipitation of submicroscopic particles of a constituent in the grain boundaries of the ferrite. Carbon steels sometimes show a sensitivity to aging effects in the postheating range which may originate from the presence of nitrogen, copper, or carbon itself. The effect of aging normally is small, but may become noticeable if cold forming precedes the welding, and if postheating is carried out in the range 500–1000° F. Some data illustrating this effect are shown in Fig. 12. It should be noted that 500 or 800° F is detrimental to the cold worked material.

VII. WHAT WE DON'T KNOW—FUTURE WORK

While the general procedures of preheating and postheating have been worked out satisfactorily in practice and the physical and metallurgical effects of these operations are fairly well understood, there are a number of areas in which further investigation is warranted. Some of these are suggested below.

Investigations in Preheating

In connection with preheating, the important practical questions are: when must preheating be used, what temperature is required and when by preheating may postheating be eliminated? While these questions gradually (but sometimes painfully) are answered by experience, experimental data would be helpful if they would provide answers to certain allied problems such as the following:

1. The relation of preheating to distortion and cracking:
   (*a*) What quantitative effect has preheating on reaction (general) stresses and local residual stresses?
   (*b*) Does preheating diminish weld cracking at high temperatures (autocracking)?
   (*c*) Is there a definite minimum temperature for each grade of steel that may be relied upon to prevent underbead cracking?
2. The relation of preheating to notch toughness:
   (*a*) Comparison of preheated vs. postheated weldments of various pressure vessel steel grades.
   (*b*) How important is it to preheat to avoid microcracks, under what range of conditions do they form and how much effect have they on notch toughness?
   (*c*) Effect of preheat temperature on the notch toughness of lower carbon steels (say below 0.18% C).

Investigations in Postheating

The practical questions which concern postheating are: How beneficial is it, and how high must the weldment be heated, also how much of the weldment must be heated? Experimental data are needed on the following points:

1. Localized vs. general postheat. How much of the weld zone must be heated to obtain the desired metallurgical effects?
2. Quantitative relations between notch-toughness and the time and temperature of postheating.
3. More conclusive data on the effects of postheating on the properties of weld metal, particularly for higher strength types.
4. The relative importance of stress-relief and metallurgical tempering as means of improving notch toughness of welded pressure vessel steels.
5. Strain-aging effects during postheating in both weld metal and base metal.
6. See also Item 2-a under preheating problems.

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Factors Affecting Resistance of Pressure Vessel Steels to Repeated Overloading

▸ *Biaxial fatigue studies in the plastic range with zero to tension vs. reversed bending; also effect of surface stress raisers, preheating, postheating and use of low-alloy steels*

by J. H. Gross, S. Tsang and R. D. Stout

INTRODUCTION

IN A PREVIOUS report[1] the design of a specimen and a machine for testing the resistance of steels to repeated loads in the plastic range was described. With this testing method a survey was made of the effects of fabrication operations such as cold work, welding and heat treatment on the resistance of two pressure vessel steels to repeated loading. Some phases for investigation suggested by the initial survey are reported here.

Relatively limited information is available on the behavior of steels under repeated overloading. Generally results of fatigue tests involving nominal stresses below the elastic limit cannot be extrapolated to the plastic range. Thus the effects of both geometrical and metallurgical variables in this field require further investigation. The following phases were studied:

(*a*) Direction of loading: zero to tension vs. reversed bending.
(*b*) Effect of surface stress raisers.
(*c*) Multibead welds with preheating or postheating.
(*d*) Effect of strength level.

An important phase of the investigation was the development of a specimen which would provide essentially 2:1 biaxial loading. Such a specimen may permit tests which can simulate conditions in a pressure vessel and thus allow the effects of fabrication operations to be evaluated without the use of expensive model vessels.

J. H. Gross, S. Tsang and **R. D. Stout** are connected with the Metallurgy Department of Lehigh University, Bethlehem, Pa.

Presented at the Thirty-Third National Fall Meeting, AWS, Philadelphia, Pa., week of Oct. 20, 1952. Closing date for discussion Mar. 15, 1953.

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THE EXPERIMENTAL PROGRAM

Testing Method

The testing machine is shown in Fig. 1. The specimen was loaded as a cantilever beam, with a cam supplying a constant amount of deflection throughout the test. Usually four levels of strain were imposed (measured at the first cycle), equivalent to 0.15, 0.4, 0.7 and 1% at the outer fibers of the test section. Loading was in equal reversed bending, except for the zero to tension tests. Triplicate specimens were tested at each level.

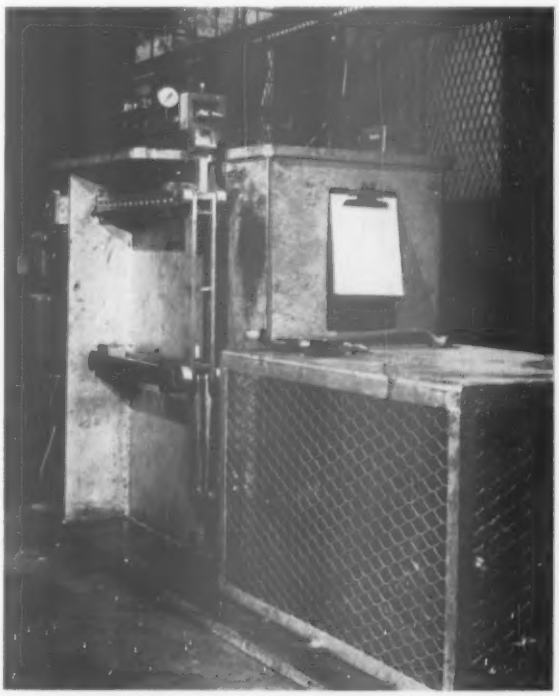

Fig. 1 View of repeated load-testing machine

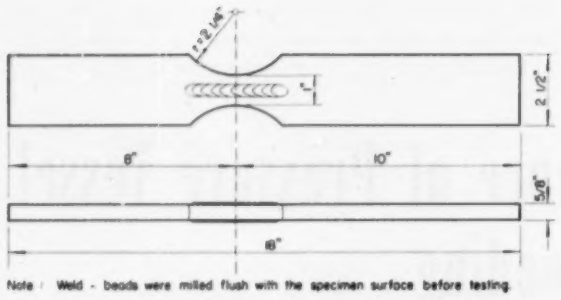

Fig. 2 Repeated load-test specimen (throated)

The original specimen design is shown in Fig. 2. This specimen had been used for previous tests and was used for the zero to tension loading series to be described later. For subsequent tests a wider specimen providing 2:1 biaxial loading was developed and substituted for the original design.

The Steels

The majority of tests was conducted on $^5/_8$-in. thick plate of the pedigreed heat of A-201 steel employed in many previous tests for PVRC. A few tests were performed on a $^5/_8$-in. plate of A-212 on hand in the laboratory. The analyses of the steels were as given below.

The steels were received in the as-rolled condition. All specimens were cut with their long axis parallel to the rolling direction.

	A-201	*A-212*
C, %	0.15	0.26
Mn, %	0.53	0.89
P, %	0.020	0.021
S, %	0.022	0.031
Si, %	0.20	0.22
Cr, %	0.04	...
Ni, %	0.05	...
Cu, %	0.07	...

Tensile properties (0.505 in. diam; 2-in. gage length)

	A-201	*A-212*
Lower yield, psi	38,000	46,000
Tensile strength, psi	62,000	78,800
Elongation, %	41	32.5
Reduction of area, %	63	53.6

Zero to Tension Loading

In previous tests, the loading of the specimen had been carried out in balanced tension and compression, obtained by bending the specimen equally in both directions. Since there are many instances in service where loading is in one direction only, a series of tests was undertaken to study the effects of zero to tension loading.

The A-201 $^5/_8$-in. plate was tested in the following conditions:

(*a*) As-received.
(*b*) 10% prestrain.
(*c*) Bead welded on the tension side (175 amp, 10 ipm, E6020).
(*d*) Bead welded on the compression side (175 amp, 10 ipm, E6020).

Strain levels were chosen for these tests such that the total deformation per cycle in zero to tension was the same as the alternating load tests. This meant that the specimens were deflected twice as much from rest but in one direction only as compared to the alternating tests.

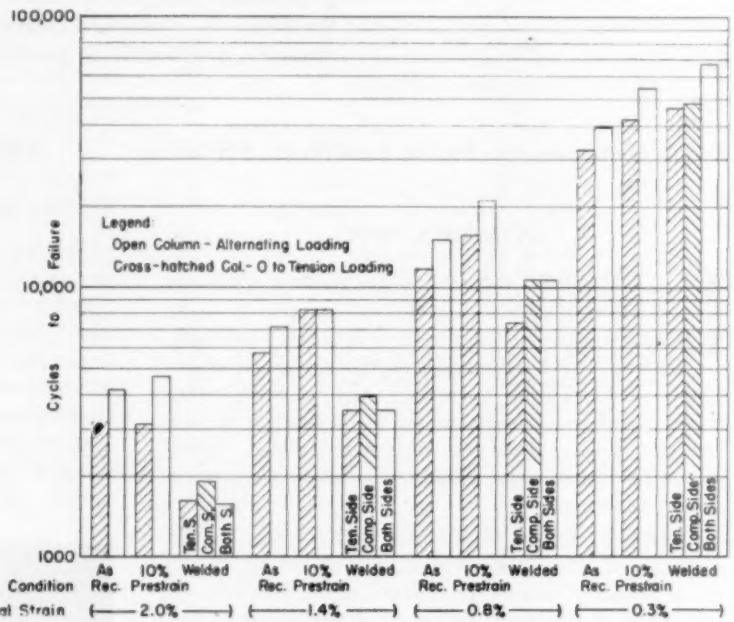

Fig. 3 Comparison of repeated load tests using alternating and 0 to tension loading

The results of the tests are shown in Fig. 3. It will be noted that the performance of tests in zero to tension was very similar to alternating load tests for the same total strain per cycle. This would indicate that in the plastic range, the amount of strain imposed per cycle is much more important than its direction. Note also that the welded specimens with the bead on the compression side were essentially no more resistant to cycling than those welded on the tension side.

These results are not unexpected. When a specimen is bent appreciably beyond the yield strain in one direction, it cannot spring back to its original position but must be forced back by undergoing additional plastic flow. After the initial cycle, the stress and strain distribution at the outer fibers will be the same

during cycling whether it is deflected 1 in. in a single direction or $\frac{1}{2}$ in. in both directions. Moreover, the compression side will have no advantage over the tension side in resisting failure.

Specimen for Biaxial Loading

It was suggested to the Lehigh staff by the Fabrication Division that attempts be made to test specimens of greater width than the 1-in. throat previously used, in order to introduce biaxial loading into the specimens. This was with the intent to parallel tests planned at the University of Illinois where a more elaborate testing procedure involving hydraulic-loaded diaphragms is to be utilized. It was suggested that a specimen having a width five times its thickness would develop essentially 2:1 biaxial strains.

When a narrow rectangular section of steel is bent into the plastic range, the section becomes roughly trapezoidal. At the outer fibers, the lateral movement is roughly 0.5 that of the longitudinal strain in obedience to Poisson's ratio. As the width is increased, a constraint to this lateral movement is developed and lateral stresses are created. A wide plate is capable of only negligible lateral flow and therefore the outer fibers will be placed under biaxial tension in which the *effective* tensile lateral strain bears the ratio of 1:2 to the longitudinal strain.

Since the A-201 plate is $\frac{5}{8}$ in. thick, a specimen width of 3 in. was adopted for the initial tests for a 5:1 width to thickness ratio. Originally a side throat was introduced to control the locus of failure, but this design had a fatal defect. Because of stress concentration at the throat radius, the cracks originated at the corners of the section instead of the mid-section where the biaxial loading should exist. Even with a large corner radius, this condition could not be eliminated. Therefore the throat section was abandoned.

After several other designs had been tried and discarded, the test specimen illustrated in Fig. 4 was adopted. This specimen has a 2-in. radius surface notch cut $\frac{1}{8}$ in. deep to localize the failure. The initial cracks formed consistently near the center of the test section rather than at the edges.

In order to obtain some measure of the biaxiality present in the specimen, SR-4 strain gages were affixed to the mid-section in both lateral and longitudinal directions. Readings were obtained by loading the specimen slowly to maximum deflection. In Fig. 5 the lateral strain is plotted as a function of the longitudinal strain. Ideally, the measured lateral strain should be zero. Actually it was about 1% that of the longitudinal strain and about 2% the amount that it should be if there were free lateral movement. Instead of a 2:1 ratio, the biaxiality was in the order of 2.1 to 1 at the maximum test strain, 1%. A similar calibration was made on a 1-in. wide specimen. The curve in Fig. 5 indicates that the biaxiality in the narrow specimen was about 4.5 to 1.

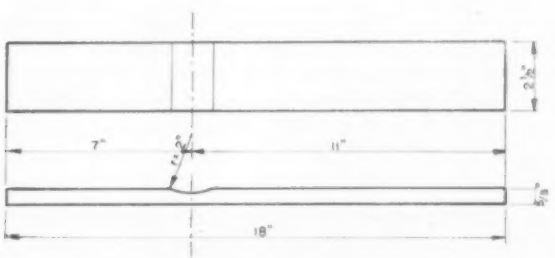

Fig. 4 Modified repeated load test specimen (surface notched)

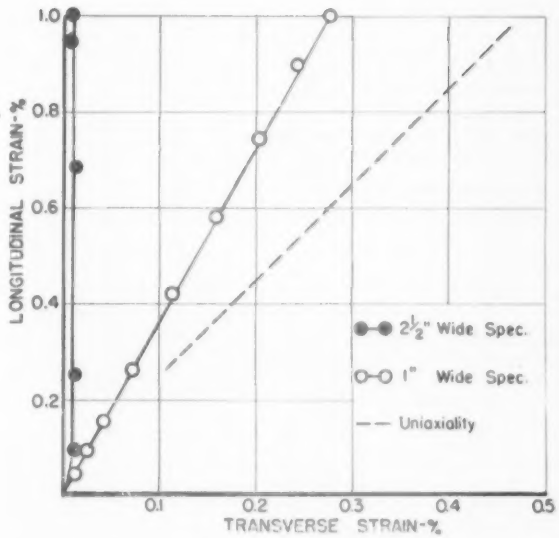

Fig. 5 Effect of specimen width on biaxiality

Since the wide specimen more nearly approximates the loading encountered in pressure vessels, and in addition is cheaper to prepare and is free of corner cracking, it was decided to adopt it for all subsequent tests. In order to compare the characteristics of the new design with the original throated specimen, a series of tests were run on (1) as-rolled plate, (2) plate prestrained 10% in tension and (3) plate with a longitudinal bead-on-plate at 175 amp and 6 ipm. As Fig. 6 shows, the effects of prestraining and welding were consistent in the two specimen designs. However, the effect of welding is somewhat less marked in the wide specimen. This may be due to the reduced fraction of the cross section represented by the weld in the wider specimen. The effects of biaxiality cannot be deduced from these tests inasmuch as the throated specimen appeared to be sensitive to the stress-raising nature of the throat geometry.

Effect of Notches

The influence of surface notches and discontinuities on the fatigue endurance limit is well known. The question may be raised whether notches will reduce the resistance to repeated loading in the plastic range to the same extent that they lower the endurance limit.

The effect of a notch was demonstrated by intro-

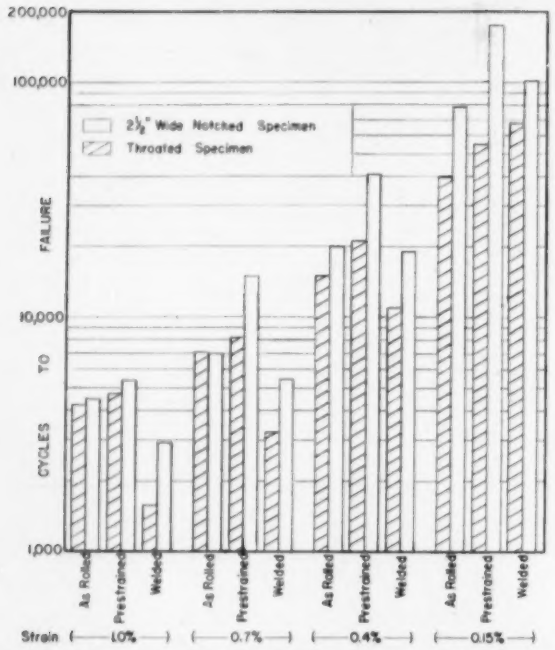

Fig. 6 Effect of welding and prestraining on behavior of notched and throated specimens

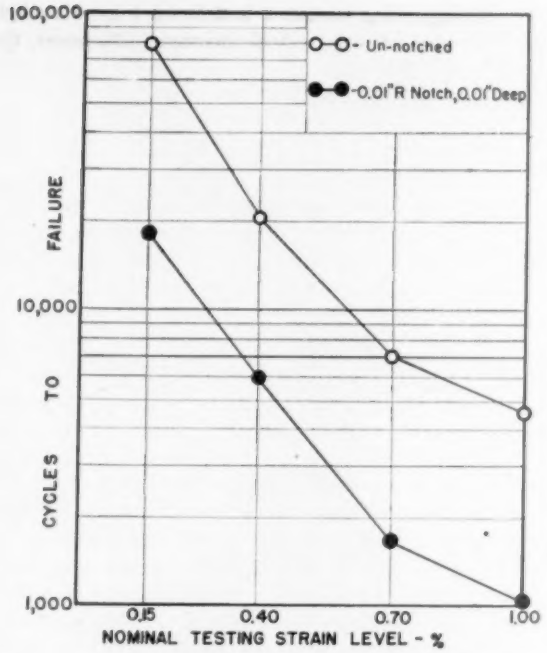

Fig. 7 Effect of notch on resistance to repeated overloading

ducing a shallow transverse notch into the test section. A V-notch of 0.01 in. root radius was cut 0.010 in. deep at the mid-point of the 2-in. radius surface groove. In Fig. 7 the performance of the notched and unnotched as-rolled A-201 steel is indicated. The influence of the relatively shallow notch was marked, resulting in a decrease of the cycles to failure of about 75% at all test strain levels.

It appears that the effect of a notch is severe in repeated plastic loading, as it is known to be in elastic loading.

Tests on Multipass Welds

The use of a single bead for assessing the influence of welding in these tests is open to the objection that the proportion of weld metal is low and no reheating is involved such as occurs in the production of a welded joint.

A deep longitudinal groove was cut into a series of specimens and filled with four beads of $^3/_{16}$-in. E6020 electrode at 175 amp and 6 ipm travel speed. Three welding conditions were included:

(*a*) Welding at room temperature with specimens half-immersed in a water bath.
(*b*) Welded with 300° F preheat and interpass temperature.
(*c*) Welded as in (*a*) with 1150° F postheat.

The results of these tests are presented in Fig. 8. The multipass welds lowered the cycles to failure to the same degree as did single-pass welds. There was, however, no improvement obtained from preheating,

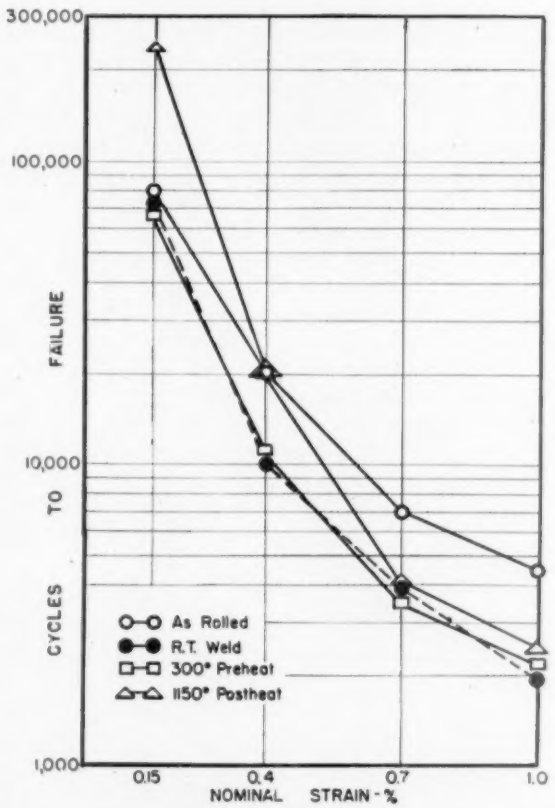

Fig. 8 Behavior of multibead welded specimens in repeated loading

and postheating was most effective for the low strain levels. Postheating had previously been shown[1] to improve single-pass beads at the high strain levels but not at the 0.15% strain level.

In order to check the soundness of the deposits, radiographs were taken of two specimens from each welding condition. The welds appeared sound except for an occasional slag stringer.

Since the surface groove removed most of the final bead, the reheating of the underpasses by the final bead may mask the effect of preheating or postheating.

Effect of Strength Level

In order to understand the alterations in resistance to repeated overloading that can be produced by fabrication operations, it will be necessary to evaluate the part played by the mechanical and metallurgical characteristics of the steel. For example, the strong effect of a notch has already been demonstrated. Cold forming raises the resistance to repeated overloads just over the yield strength, probably because of the work-hardening accompanying cold work. The relative importance of the strength and ductility of the steel may shift as the testing strain level is changed.

The effect of the tensile strength of the steel on its behavior under repeated loading is of special interest. The strength may be raised by alloying, by heat treatment, or by cold work. How the repeated loading properties in turn are affected may possibly depend on metallurgical changes accompanying these treatments. More information is needed on this subject.

As a beginning a series of specimens was prepared in which the strength level of the A-201 steel was varied by cold work and by heat treatment. A second steel, A-212, was added to introduce the effect of composition.

The conditions studied were as follows:

(*a*) A-201 steel, as-rolled.
(*b*) A-201 steel, annealed at 1600° F.
(*c*) A-201 steel, water-quenched from 1600° F, tempered 1150° F.
(*d*) A-201 steel, prestrained 10% parallel to rolling.
(*e*) A-212 steel, as-rolled.
(*f*) A-212 steel, oil-quenched from 1600° F.

All treatments were performed on the steel cut to specimen size. All of the specimens were tested at the same deflection settings on the machine corresponding to the calibrated strain levels of 0.15, 0.4, 0.7 and 1% as measured on the as-rolled, A-201 steel.

The results of these tests are summarized in Fig. 9. In this same figure the tensile properties for each condition are also included. It will be noticed that for a test strain level of 0.15%, the yield or tensile level correlates with the cycles to failure. At higher strains, the order becomes mixed. The A-212 steel was consistently better than the A-201 steel, but the 10% prestrained A-201 which attained a tensile approaching that of the as-rolled A-212 likewise matched it in repeated load resistance.

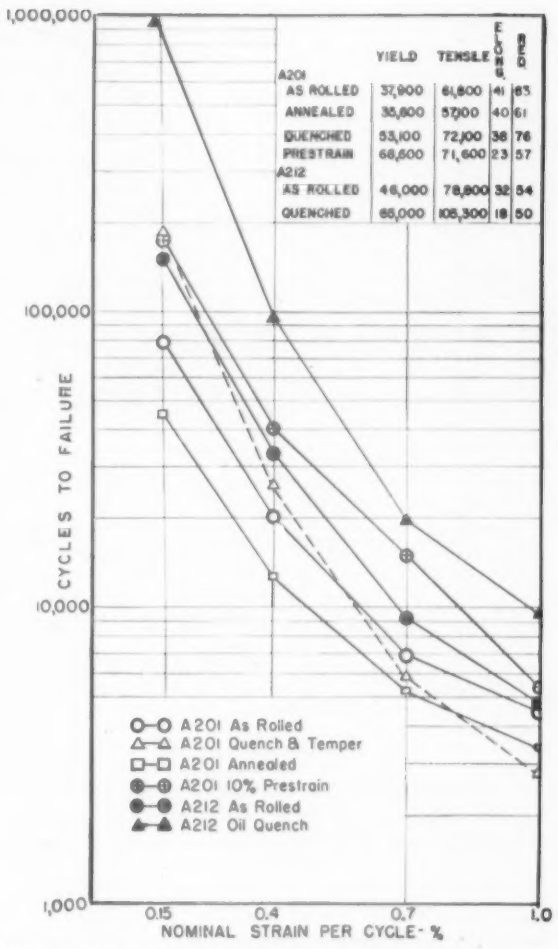

Fig. 9 Effect of tensile strength on resistance to repeated loading

The role of tensile strength is shown more clearly in Fig. 10, in which the cycles to failure are plotted against the tensile strength of each of the six series of specimens. At a test strain level of 0.15%, there is a noticeable and consistent effect of tensile strength on the cycles to failure. At 1% strain, the slope of the curve becomes more nearly level, but still indicates an advantage for higher tensile levels.

In Fig. 11, ductility values in the form of percent elongations are plotted against cycles to failure. The steels of higher ductility show reduced repeated load resistance. This does not mean, of course, that ductility in the steel is unfavorable for repeated loading, but rather that a gain in tensile strength at the sacrifice of some ductility is beneficial. This is true even at the relatively large plastic strain level of 1%. Such results were not to be expected, and therefore a further study was made of the conditions present during testing.

Effect of Tensility on Strain at Constant Deflection

Since it did not seem reasonable to expect tensile

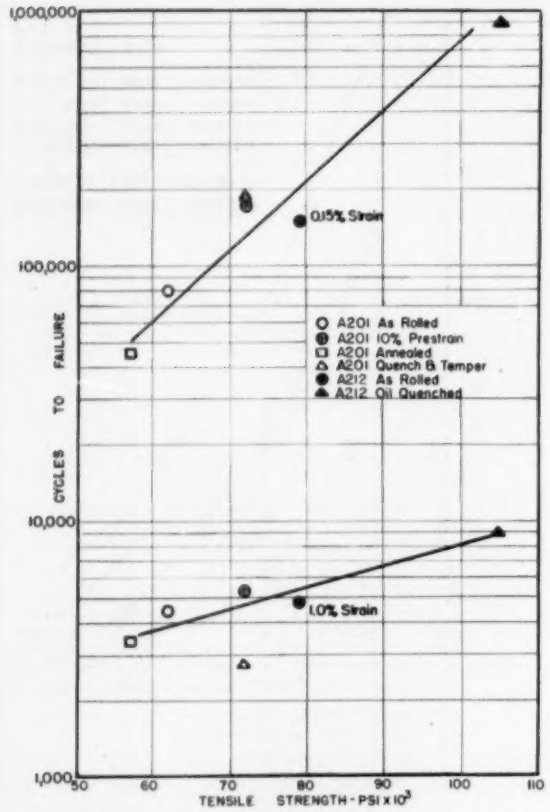

Fig. 10 Correlation of tensile strength with resistance to repeated loading

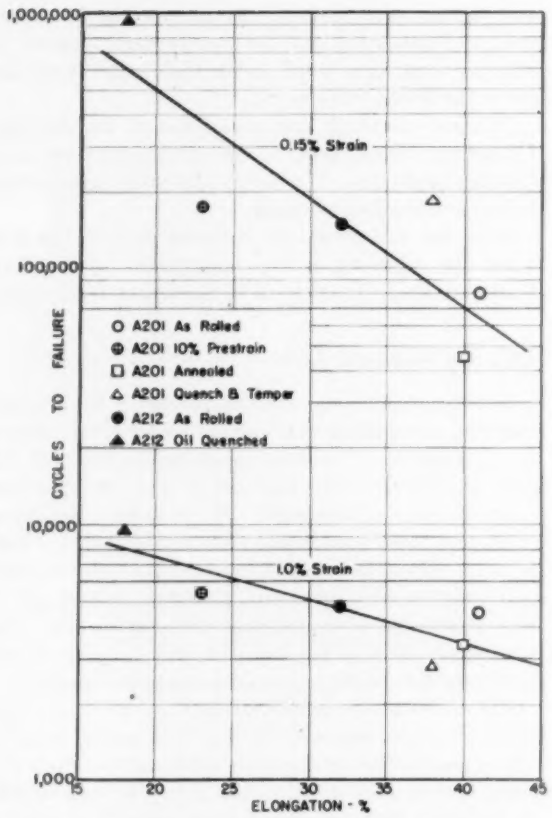

Fig. 11 Correlation of ductility with resistance to repeated loading

strength to be more important than ductility at high levels of test strain, a further calibration of strains for specimens of various tensile strengths was undertaken. For this purpose a Tuckerman optical gage was utilized. The gage length was $^1/_4$ in., and the measurements were taken at the minimum section of the surface groove. The error introduced by the curvature of the surface (2 in. radius) was neglected, since it was small and constant for all specimens.

The following series of specimens was calibrated:

(*a*) A-201, $^5/_8$ in. thick, as-rolled.
(*b*) A-201, $^5/_8$ in. thick, prestrained 10%.
(*c*) A-201, $^5/_8$ in. thick, brine quenched, tempered 1150° F.
(*d*) A-201, $^3/_4$ in. thick, normalized 1650° F, tempered 1150° F.
(*e*) A-302, $^3/_4$ in. thick, normalized 1650° F, tempered 1150° F.

The strain on the initial tension cycle vs. the deflection produced at the moving end of the specimen (10 in. from the fixed vise) is plotted for each specimen in Fig. 12. It will be noted that, in the $^5/_8$-in. A-201, as the deflection is increased the strains obtained in the higher-tensile prestrained or quenched specimens were noticeably lower than in the softer as-rolled specimen. Similarly the $^3/_4$-in. A-302 alloy steel, with a tensile

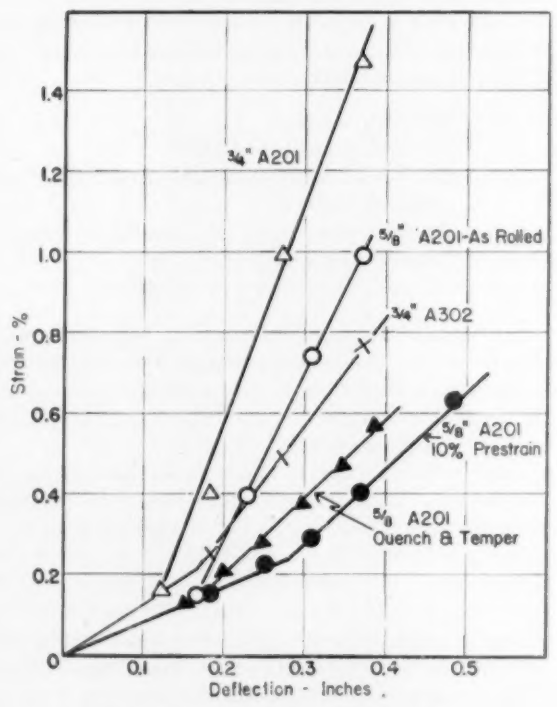

Fig. 12 Strain at first tension cycle

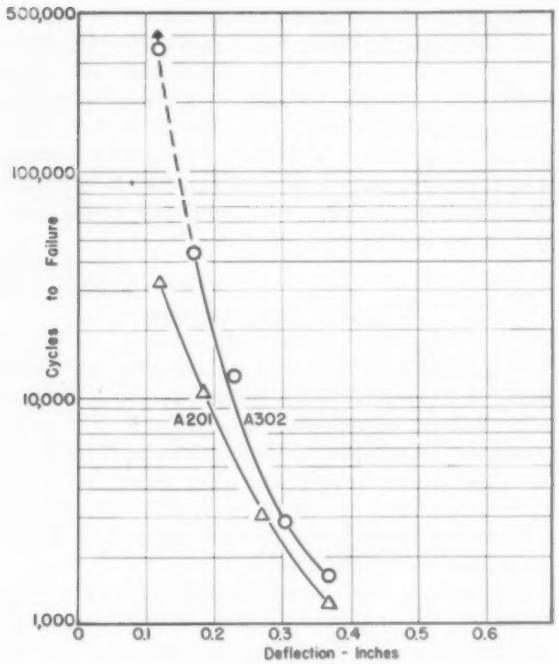

Fig. 13 Comparison of A-201 and A-302 at equal deflections

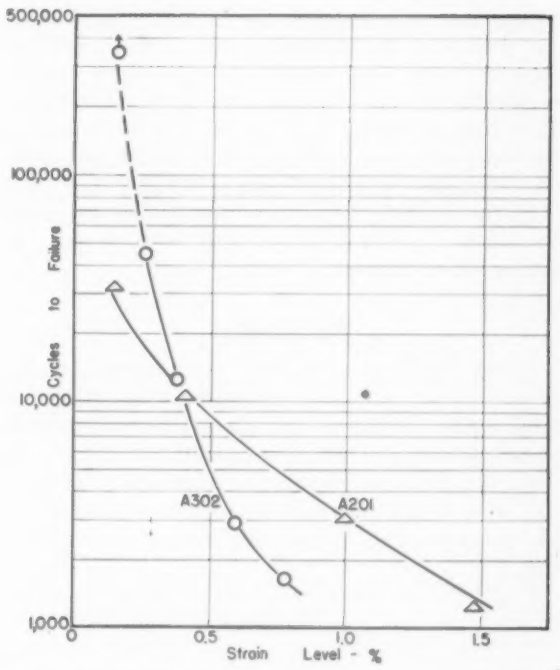

Fig. 14 Comparison of A-201 and A-302 at equal strains of first tension cycle

strength of 80,000 psi was strained appreciably less at large deflections than the $^3/_4$-in. A-201 with a tensile strength of 60,000 psi. Thus, when straining greatly exceeded the elastic range, the test section of the weaker steels experienced a relatively larger plastic strain than did the stronger steels for the same external deflection of the specimen.

From these measurements it can be concluded that the superiority of the high-tensile specimens arose from the greater resistance of the test section to plastic straining. Although the first thought is to correct the data of Fig. 9 for the actual test strains imposed, this step may not be fully justified. In an actual vessel, a given pressure causes a corresponding elastic deformation in the vessel. The elastic deformation in the vessel can be compared to the deflection imposed on test specimens. While the over-all deformation of the vessel is elastic, stress-raisers in the vessel can cause local plastic straining, just as occurs in the test notch of the laboratory specimen. Now the magnitude of the local plastic strain may be affected by the tensile strength of the steel in the pressure vessel in the same way as was exhibited in the test specimens. That is, high tensile strength may be as beneficial in resisting stress raisers in actual vessels as it was observed to be in these tests. This point must be investigated further.

In order to illustrate the relation of tensile strength to repeated load resistance at corrected test strain levels, the data for normalized and stress relieved $^3/_4$-in. A-201 and A-302 steels were replotted in Figs. 13 and 14. In Fig. 13, with deflection as the variable, A-302 is superior to A-201 at all test levels. In Fig. 14, with the calibrated test strains as the variable, the A-302 steel retains its advantage at low test strains, but becomes noticeably poorer than the A-201 steel at high test strains. The latter is the expected relation, but not necessarily the most significant one.

CONCLUSION

The resistance of steel to repeated loading in the plastic range appears to be about as sensitive to design factors and the tensile strength level as is the endurance limit. Further work is required to establish the influence of variables such as yield strength, ductility and metallurgical factors of microstructure and composition. Such information may be useful in connection with the proposed use of higher tensile steels in pressure vessels.

The testing machine for this investigation has been operated to impose a constant amplitude of deflection on the specimen. This means that a nominally constant amount of plastic strain was induced in the specimen during the test as opposed to the constant load generally employed in fatigue tests. There are advantages and disadvantages to both constant deflection and constant loading methods for testing in the plastic range.

In a pressure vessel the critical regions so far as potential failure is concerned are the changes in section, openings, and the unavoidable notches introduced during forming and welding. Under loads which will cause only elastic strains in the body of the vessel, these critical regions, because of their stress-raising

nature, may experience plastic flow. During loading and unloading the elastic movement of the vessel can force the local zones containing stress-raisers to flow plastically in alternate tension and compression. Thus there is just as much reason to select a testing method which uses a fixed amplitude of deflection as to use a constant amplitude of load.

In future tests, particularly for studies involving loads just over the yield strength, both methods of testing will be included.

SUMMARY

1. In repeated overloading, the total amount of strain imposed per cycle appeared to have about the same effect whether it was applied in balanced reversed bending or in one direction only.

2. A specimen having a width to thickness ratio of 5 to 1 was found to be suitable for producing essentially 2 to 1 biaxial loading in the outer fibers of the test section.

3. Multibead welds were found to lower the cycles to failure to about the same extent as single-bead welds.

4. The introduction of a relatively shallow notch lowered the resistance to repeated overloading 75% at all levels of strain.

5. The tensile strength as varied by cold work, heat treatment or steel composition controlled the cycles to failure at all strain levels. At low test strains the cycles rose markedly with tensile strength, while at high strains the increase was less marked. Since high tensile strengths resulted in a loss in ductility, the improvement at 1% test strain level was unexpected. Calibration of specimens possessing a range of tensile strengths revealed that these results were due to reduced straining levels developed in specimens of high tensile strength.

ACKNOWLEDGMENTS

This work was sponsored by the Pressure Vessel Research Committee of the Welding Research Council, which is directed by William Spraragen. P. R. Cassidy is Chairman of the Pressure Vessel Research Committee and Boniface E. Rossi is Executive Secretary. F. L. Plummer is Chairman of the Fabrication Division of the Pressure Vessel Research Committee, which guided the project staff in this work.

The project was carried on jointly by the Fritz Engineering Laboratory of the Civil Engineering Department, and the Metallurgy Department of Lehigh University.

The execution of work was made possible by the cooperation of Kenneth R. Harpel, laboratory foreman and the entire laboratory staff.

Prizes for Films and Papers on the Oxy-Acetylene Flame

At their 35th meeting at Stockholm early in 1952, the Permanent International Committee on Acetylene Gas Welding and Related Industries decided to organize two international contests for films and papers on applications of the oxy-acetylene flame. The regulations governing the two contests are given below. Everyone is invited to participate in the contest.

Films. The film must show one or several practical applications of the oxy-acetylene or air-acetylene flame such as welding, brazing, cutting, heat treating, surfacing, metallizing, etc. It can be entirely original or composed in whole or in part of cuttings from other films acquired by the author. Each competitor may present several films. The films must be 16-mm. silent in black and white, or in color with titles in English or French. The maximum time of projection is 15 minutes. The films must give no indication of their origin or author, but must bear a code number. The films must be accompanied by a description of their contents. The description must bear the same title and code number as the films and should state the time of projection, as well as the exact way in which cuttings have been made from other films. The description must not reveal the author. It must be written in English or French; typewritten with five copies on one side of paper 21 x 29.7 cm. Each film with its description must be sent in an envelope labeled, "Film", and code number only. This envelope should also contain a sheet of paper showing the name, address and code number of the author. The envelope should be addressed: Concours de films 1953, Secrétariet Général de la Comission Permanente Internationale de l'Acétylène et de la Soudure Autogène, 32 Boulevard de la Chapelle, Paris 18, France. The contest is open from January 1st to Dec. 31, 1953. There will be three prizes totaling 5000 Swiss Francs ($1160). The films will be judged by a jury consisting of members of the Oxy-acetylene Subcommittee of the CPI, with, if necessary, a film expert. The distribution of the prizes will be entirely in the hands of the jury. The prize films will become the property of the CPI, with the author's name always being mentioned.

Papers. The papers must deal with one or several practical applications of the oxy-acetylene or air-acetylene flame such as welding brazing, cutting, heat treating, surfacing and metallizing. They must not have been published before Jan. 1, 1953. The papers must be in English or French, with five typewritten copies on one side only on paper 21 x 29.7 cm. Five copies of all illustrations and diagrams also must be sent. A paper must not exceed 20 typewritten pages, single spaced, not including illustrations, and must be accompanied by an abstract briefly stating the object of the work and the results obtained. The paper must carry a code number, but not the name of the author. Five copies of each paper, accompanied by illustrations, must be placed in an envelope labeled "Mémoire." The envelope must also contain another sheet containing the code number and name and address of the author. The envelope should be addressed to: Concours International 1953, Secrétariat Général de la Commission Permanente Internationale de l'Acetylène et de la Soudure Autogène, 32 Boulevard de la Chapelle, Paris, 18, France. The contest is open from January 1st to Dec. 31, and there will be three prizes totaling 5000 Swiss Francs ($1160). The papers will be judged by a jury composed of members of the Oxy-acetylene Welding Subcommittee of the CPI, and their decision is final. Papers awarded prizes become the property of the CPI.

Report on the Design of Pressure Vessel Heads

An interpretive report based largely on the laboratory researches and theoretical investigations sponsored by the Design Division of the Pressure Vessel Research Committee

SECTION 1—INTRODUCTION

THE Pressure Vessel Research Committee of the Welding Research Council was organized in 1945, and consists of four Divisions: Materials, Design, Fabrication, and Inspection and Testing.

The Design Division first met in the Spring of 1946 and, after viewing the problems that could be undertaken, decided that its principal field could be divided into three essential branches of design: (1) heads and their attachments to pressure vessel shells, (2) reinforced openings in pressure vessels and (3) supports and other attachments which apply external loads to pressure vessels. Limited funds precluded the possibility of starting programs of research on all three phases at once.

A great deal of theoretical work had previously been done on the design of heads and their attachments to shells, much of it recently by the Standard Oil Co. (Ind.). To verify these theoretical results, experimental work was undertaken by the Design Division at Purdue University. It was specified that this experimental program should be carried beyond the elastic into the plastic range.

At about the same time, the Taylor Forge and Pipe Works agreed to further, with its own funds, any needed theoretical work and experimental studies on reinforced openings in models and test vessels, and to make the results available to the Design Division of PVRC. This very welcome offer was accepted, and the work with regard to models is now under way.

It was agreed to postpone the Division's planning on supports and attachments until the work on heads was well under way so as not to spread too widely the limited resources of the Division. Because of that decision, this Interpretive Report of the Design Division deals mainly with the design of heads and their connection to shells. The work on the other two proposed branches of research has not yet reached the stage where an interpretive report can be issued, but satisfactory progress is being made.

Prepared by a group consisting of H. C. Boardman (C. E. Carlson, L. P. Zick), W. R. Burrows (R. L. Graves, P. G. Stevens), P. R. Cassidy, F. L. Maker, J. J. Murphy, C. O. Rhys, B. E. Rossi, Walter Samans, G. J. Schoessow (D. K. Davies), L. W. Smith, W. Spraragen, R. G. Sturm, E. O. Waters and G. W. Watts (W. M. Dudley).

SECTION 2—HISTORICAL BACKGROUND

The study of stresses in heads was started in Europe some sixty years ago and later was undertaken in the United States as well. The details of these studies are given in Appendix I.

SECTION 3—THEORETICAL WORK BY DESIGN DIVISION

(Analytical work to date is based for the most part on elastic theory. The Design Division, however, recognizes that what happens in the plastic region is just as important, and possibly more so in practical pressure vessels.)

When the Design Division study of the pressure vessel head problem was initiated in 1946, each of the several accurate or approximate and apparently different analyses then in use for straight or curved shells had a considerable following of its own. A literature survey and comparative theoretical studies made at Purdue University in the summer of 1946 showed that these theories were actually identical in background, being either approximate or accurate solutions of the Love-Meissner analysis of thin pressure vessel heads and shells, an analysis which is itself approximate. At that time, 1946, Watts and Burrows proposed the use of accurate, series type solutions of the Love-Meissner analysis to serve, when calculated, as a basis for the preparation of engineering type tables and curves showing the stresses and deformations in well-known head shapes in a range of head proportions for which these accurate series type solutions are suitable. They suggested that these precise results would also serve as indexes of the degree of accuracy of shorter, less rigorous methods, and would provide indications of the limitations of such approximations. In 1947,

Watts and Burrows prepared ASME paper 47-A-153, "The Basic Elastic Theory of Vessel Heads under Internal Pressure," (published in the ASME *Jnl. Applied Mechanics* for March 1949, pp. 55–73) to outline the fundamentals of the Love-Meissner approximate theory of thin shells; to establish methods for the calculation of shell-head boundary problems; to show that the Love-Meissner and the Poisson-Kirchhoff theories of uniform flat heads are identical; to record the Bartky-Burrows 1946 improvement in the rate of convergence of Bolle's hypergeometric series solutions for spherically shaped heads; and to tabulate those formulas needed to perform precise calculations with the convergent infinite series involved. Bartky's work (the use of Kummer's quadratic transformation), as improved by Burrows, is a definite Design Division contribution to the solution of spherical heads because Bolle's work, cited in English by Timoshenko, is not directly applicable to hemispheres beyond a distance of about 45 deg from their centers.

The accurate computations just described have been under way since 1948, under the direction of Watts, Dudley and Lang, Engineering Research Department, Standard Oil Co. (Ind.). Before calculating started, they made an extremely careful recheck to assure the correctness of all the formulas involved. A calculating procedure was then established. Desk computers proved to be sufficient for flat, spherical and conical heads, but the New York facilities of IBM (International Business Machines Corp.) had to be employed for toroidal knuckles with circular sections. The contemplated work is now about two-thirds completed. The flat head is covered by ASME paper 51-A-146, "The Stresses in a Pressure Vessel with a Flat Head," by Watts and Lang, 1951; the conical head is covered by "The Stresses in a Pressure Vessel with a Conical Head," ASME paper 51-PET-8, by Watts and Lang, 1951, *Trans., ASME* April 1952, pp. 315–326; and the hemispherical head is covered by "The Stresses in a Pressure Vessel with a Hemispherical Head," by Watts and Lang, 1951, presented as an ASME paper on June 16, 1952, at Cincinnati, Ohio. The work on the series coefficients for the toroidal knuckle with circular section has been completed in so far as IBM is concerned, but further calculations are required to apply these IBM results to toriconical and torispherical heads. In the dimensional ranges covered, all of these results are entirely suitable for engineering use. They also constitute a store of excellent check values so acutely needed to establish the relative accuracy of approximate procedures.

Geckeler type approximations appear more and more to be the answer to a search for shorter calculation methods immediately applicable over a much broader range of head sizes and shapes than those methods based either on solutions in series or on numerical integrations. Moreover, the Geckeler type approximations provide the engineer with complete and readily usable formulas in an algebraic form that is suitable for an entire group of heads of the same general shape. All other accurate or approximate solutions of the Love-Meissner equations apply only to specific shapes and proportions of heads having the particular ratios of diameter to thickness involved in the computations; and results for heads of other proportions and other ratios of diameter to thickness must be obtained by interpolation or extrapolation. Therefore, of the approximation schemes now known, the Geckeler type seems to be the most desirable for engineering use whereever it is applicable. Geckeler type approximations are now being used in a number of forms that probably differ somewhat in the degree of accuracy of the results produced. These basically identical approximate methods are sometimes called Geckeler approximations and are sometimes referred to as beam-on-an-elastic-foundation methods. Nevertheless they are representative of the same general procedure. Obviously what is sought for engineering use is the most readily applicable of all the Geckeler forms now proposed, and this particular Geckeler solution is being established by Stevens, Graves and Burrows, Development Department, Standard Oil Co. (Ind.), by means of an orderly process of eliminating all terms that are found to be without numerical significance from an engineering point of view. Checks of the Geckeler approximation, that are now entirely complete, are for the locality of the junction between the hemispherical head and the cylindrical shell. Although the discontinuity stresses here are too small to have very much engineering significance, nevertheless this fact does in no way invalidate the check since both the accurate solutions in series and the Geckeler approximations show almost identical numerical values for these low stresses. Much more work, however, remains to be done.

A subcommittee of the Design Division, consisting of Waters, Rhys and Sturm, has worked out improved stress distribution assumptions for the cone-cylinder junction. These results consist of a method for harmonizing the stresses at this head-to-shell junction by assuming a narrow ring attached on one side to the shell and on the other side to the cone head. The work indicates that stress concentration factors, called stress indexes, can be computed by this method for various diameter-thickness ratios for any given type of junction but, for practical designing, this refinement probably is unnecessary. The method has been tested by applying it to a shell attached to a 45-deg conical head of the same dimensions as Purdue Model No. 3 (a near zero radius of transition section between shell and head; head thickness equal to 1.41 times the shell thickness; and a diameter-to-thickness ratio equal to 35 to 1). The agreement with test results is entirely satisfactory.

In its present form, it is felt that this method is unduly complicated and requires too much computing. With practice, and further comparison with tests, it should be possible to eliminate unnecessary refinements and discover shorter procedures. This will render the method more palatable to the pressure vessel designer who needs to go beyond the elementary rules for design

and find out what stress concentrations, if any, exist when the shell-to-head junction in the conical head is extremely abrupt. Results of three-dimensional photoelastic studies at Purdue may also be helpful in this effort

Boardman, Carlson, and Zick of the Chicago Bridge and Iron Co. have prepared a Monograph on "Rules for Pressure Vessel Heads." The paper is divided into four main divisions: Introduction; Analysis of 1950 Code rules; The General Theory of the Stress Analysis employed; and Discussions, Conclusions and Recommendations. The method employed is that of a beam on an elastic foundation (the Geckeler approximation), and this procedure is used to develop rules for the calculation of elastic stresses* in hemispherical, ellipsoidal, torispherical and conical heads. Many tables, curves, and diagrams are given, and the whole paper, consisting of some fifty typewritten sheets, represents a very great amount of valuable work. The results agree quite well with the Purdue experimental data. The 1950 ASME Code, Section VIII, Rules of Head Design, are analyzed in such a way as to relate closely the head types. The profile is worked out for that particular torispherical head which requires the least thickness of metal for a given ratio of diameter to depth. It is interesting to note that this particular result shows marked agreement with conclusions reached in ASME paper PME-53-7 by Rhys, 1931. Relative to this CB&I Monograph, results are given in the main body with details in appendices and the following papers are cited: "Stresses at Juncture of Cone and Cylinder in Tanks with Cone Bottoms or Ends" by Boardman, *The Water Tower*, January 1944; "Stresses at Junction of Two Right Cone Frustums with a Common Axis" by Boardman, *The Water Tower* March 1948; and "Optimum Torispherical Pressure Vessel Heads" by Boardman, *The Water Tower*, January 1949.

Since 1946, Rhys has contributed some fifteen papers dealing with various extremely ingenious applications of the Geckeler method. The more significant of these are listed in the Bibliography, Section 8. In many cases Rhys uses Geckeler type formulas that are presumably somewhat more accurate than those proposed by Geckeler. With these papers should be included the aforecited ASME paper, PME-53-7 by Rhys, 1931, which agrees with the Purdue experimental results for torispherical heads, although the paper's validity was considerably questioned at the time it was written in 1931. This 1931 work by Rhys comprises about 100 typewritten pages, and contains many curves and tables for torispherical heads. All the work shows very good agreement with the Purdue experimental results, and with the experimental results given in ASME paper 49-F-19 by Brooks and Schoessow, Babcock and Wilcox Company, 1949.

In an unpublished 1950 Design Division Paper, "Pressure Vessel Heads under Internal Pressure, Part I, General Considerations," by Burrows, the use of Love's elastic theory of thin shells is tentatively limited to shells thinner than D/T equals 30 on the basis that this theory is not suitable for relatively thicker shells; the derivation of the Love-Meissner solution, eliminated for the sake of brevity from ASME paper, 47-A-153, by Watts and Burrows, is presented; all general results for the Love-Meissner method are recorded for straight and curved shells, including the Meissner and Normal Form equations; the derivation of the best-known Geckeler solutions is developed; methods are outlined for suitably modifying Love's theory to improve its applicability to very thick shells (D/T less than 30); and a reasonably complete bibliography is included. Its contents may be of particular interest to students of the theory.

Another pertinent paper is ASME paper 49-F-20 by L. F. Kooistra and R. U. Blaser, Babcock and Wilcox Co. This paper describes experimental techniques in pressure vessel testing.

SECTION 4—EXPERIMENTAL WORK BY DESIGN DIVISION AND ITS CHECK WITH THEORETICAL COMPUTATIONS

In order to check the theoretical computations made by the Manufacturing Engineering Department of the Standard Oil Co. (Ind.) and others, the Design Division, PVRC, prepared a comprehensive test program, and contracted with Purdue University to carry out the experimental work.

The following is a summary of the testing work done at Purdue:

Strain gage tests of six 8-in. diam steel models and two 48-in. diam steel vessels were completed (see table 1). These tests were carried beyond the point of local yielding. In addition to these, two photoelastic model vessels and two shell-reducer models were tested. Results of the tests on the 8-in. diam steel models indicate that for the case of a conical head with no knuckle radius the measured elastic stresses* are in close agreement with the stresses computed by the Love-Meissner analysis or the Love-Meissner approximation. The tests on toriconical heads, with a "minimum" knuckle radius equal to 6% of the shell diameter,

Table 1—Sizes of Eight Steel Vessels Tested at Purdue University

Type of head	*Mean diameter, in.*	*Shell thickness, in.*	*Head thickness, in.*
Conical	7.178	0.200	0.283
Conical	7.6	0.100	0.100
Toriconical	7.178	0.200	0.200
Toriconical	7.178	0.200	0.283
Toriconical	48.632	0.632	0.740
Toriconical	48.625	0.625	0.875
Torispherical	7.6	0.100	0.100
Torispherical (ellipsoidal)	7.6	0.100	0.100

* The term "elastic stresses" means stresses due to elastic strains.

* The term "measured stresses" means stresses calculated from measured strains. The term "measured elastic stresses" means stresses calculated from measured elastic strains.

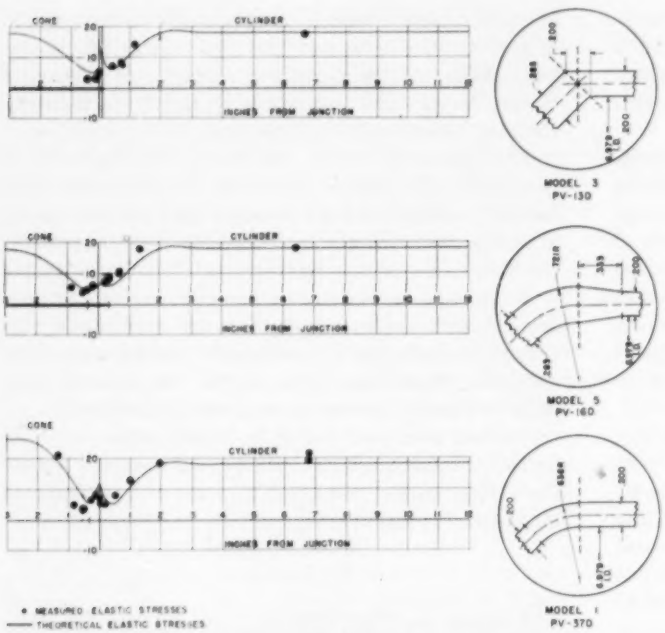

Fig. 1 Measured and computed inner circumferential stresses—Models 1, 3 and 5

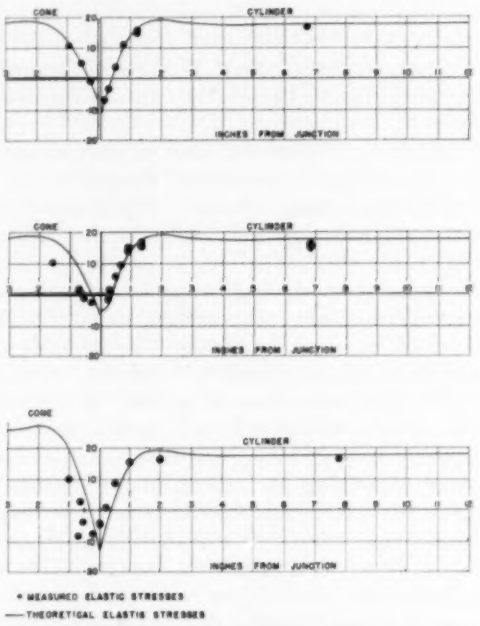

Fig. 2 Measured and computed outer circumferential stresses—Models 1, 3 and 5

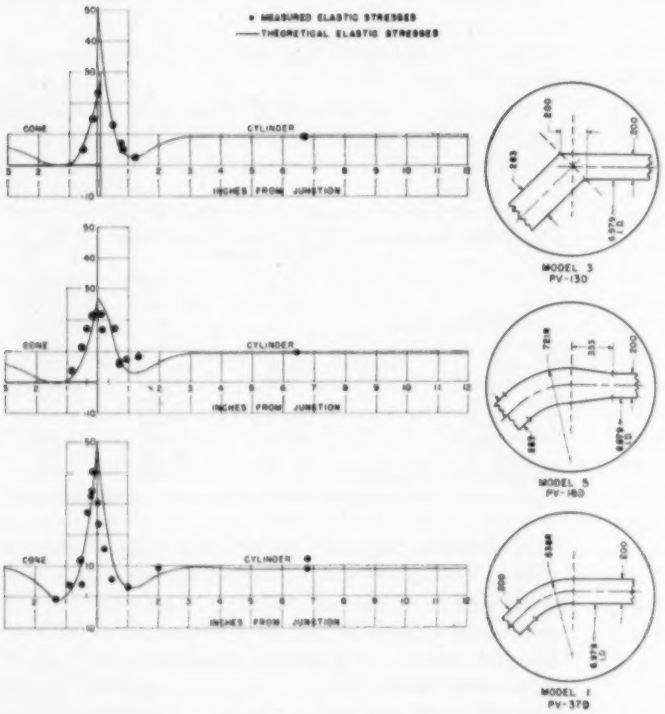

Fig. 3 Measured and computed inner longitudinal stresses—Models 1, 3 and 5

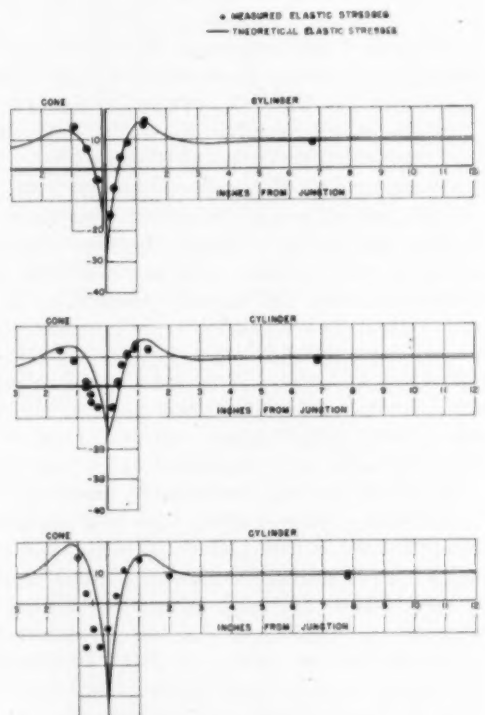

Fig. 4 Measured and computed outer longitudinal stresses—Models 1, 3 and 5

revealed that peak stresses measured in the knuckle region are somewhat smaller than the peak stresses predicted by the approximate theory for conical heads. Figures 1 to 4 show the comparison of theoretical elastic stresses and experimentally measured elastic stresses at the head-shell juncture for three 8-in. models having 45-deg conical heads.

Strain gage tests on the two 48-in. diam vessels revealed that the general trend of the stresses in the larger vessels is in close agreement with that in the models. The measured stresses are also close to those computed by the approximate theory for conical heads, indicating that this approximation is sufficiently exact for engineering use. Fabricating irregularities in the 48-in. vessels caused only slightly greater stresses than occurred in comparable places where no irregularity existed.

Tests on the weld metal revealed that it had a higher yield point than the base metal of the vessels. If welds are placed at the sharp cone-cylinder junctures, less local yielding and higher absolute values of tensile stress are observed than would be the case if there were no welds at the juncture.

Two 8-in. steel models with torispherical heads were tested. One torispherical head was approximately elliptical in section (2:1), while the other was a "dished" or "basket" head having a 6% corner (knuckle) radius joining the crown and cylinder. The crown radius was equal to the cylinder diameter. A comparison of strain gage test results showed:

(*a*) The "dished" head had much higher stress concentration than the approximately "ellipsoidal" head.

(*b*) The maximum stress, determined experimentally for the approximately "ellipsoidal" head, was an inner axial stress near the point of tangency of the two component simple curves that approximated the ellipse.

(*c*) The maximum measured stress for the "dished" head was an inner axial stress in the knuckle.

(*d*) More serious bending stresses at the juncture of the cylinder and head were observed for the "dished" head than for the approximately "ellipsoidal" head.

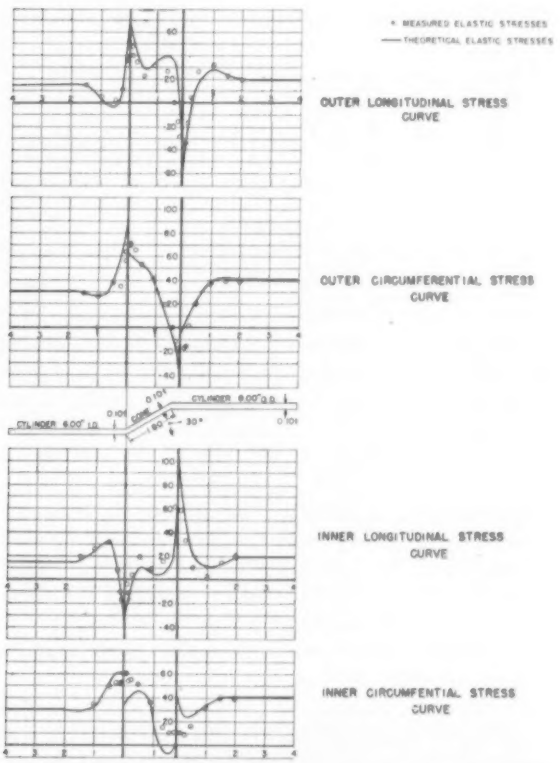

Fig. 5 Measured and computed stresses—Shell reducer Model 1

Conical transition reducer sections with and without corner radii are often used in pressure vessels to connect two shell sections of different diameters. The stress conditions at the large diameter end can be analyzed and designed as for complete conical heads. The small diameter end has not received adequate attention up to the present and so the Design Division decided to investigate it. A reverse curve reducer will also be tested.

The strain gage tests of two shell-reducer models have been completed. The first model had a conical section joined with sharp corner juncture to the cylinders of 8- and 6-in. diam, and in the second reducer a conical section with a radius on each end was joined to axially aligned 6- and 8-in. cylinders. A Geckeler type (approximate) analysis was derived for the first shell-reducer. Figure 5 shows the comparison of experimental values and the theoretical curves. In general, the agreement is good. No analysis was made for the second shell reducer. A comparison of the test results shows that, in general, the peak measured stresses for the second shell-reducer are smaller than those for the first. The peak circumferential stress in the toriconical reducer is located at the outer surface near the center of the torus segment which adjoins the 6-in. cylinder, while the peak circumferential stress in the simpler conical reducer occurs at the sharp junctures. Figure 6 shows the comparison of measured elastic stresses for both reducer sections and indicates graphically the location of the peak stresses. The high average circumferential tensile stress at the reentrant intersection is to be particularly noted. On carrying this test to destruction, this section was the first seriously to deform.

Photoelastic investigations of two models have been completed. The first model was one with a toriconical head, while the second was one with a conical head having sharp corner junctures. Emphasis in photoelastic analysis was placed upon determining the stress distribution through the thickness of the vessel wall at and near the junction. However, inner and outer longitudinal stress values were determined and they show about the same agreement with the approximate analysis as do the strain gage tests. It is interesting to note that the fringe photograph of an axial slice

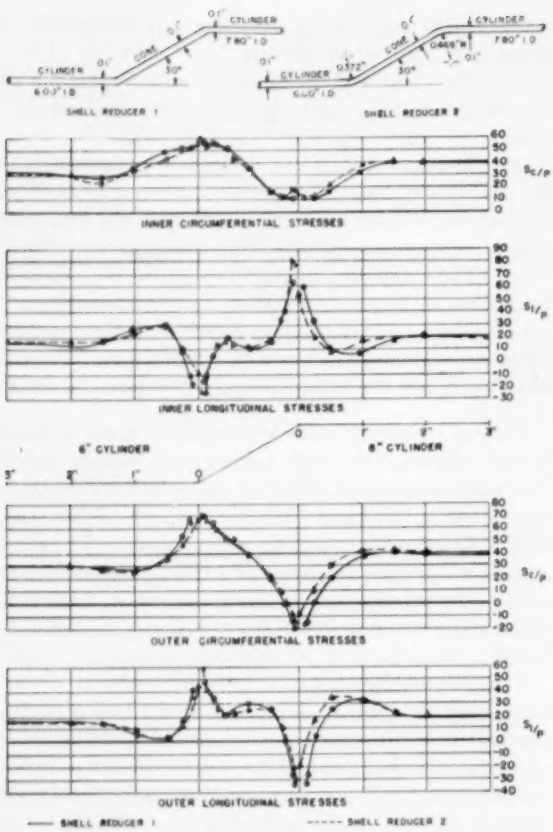

Fig. 6 Comparison of measured stresses in shell reducers

through a conical head model (Fig. 7) shows a zero fringe, and therefore zero longitudinal stress for this model at the outer edge of the sharp juncture. The stress concentration at the inner junction is also vividly shown.

In connection with the work of the Design Division, the Purdue Project staff plotted design curves (using the Geckeler approximation) for local bending elastic stresses at the sharp-cornered junction of the cone and cylinder for cylindrical pressure vessels with cone heads. These curves, such as the one shown in Fig. 8, are in the form of a dimensionless ratio containing local bending stress plotted against θ, one-half the apex angle of the cone head. Each curve is for a particular one of the H/h (the cylinder-to-head thickness ratio) values ranging from 0.50 to 1.50 with several curves for m, which is a function of D/t, mean diameter to thickness of shell element. These curves represent one way of presenting the results of a complex analysis in simple graphical form.

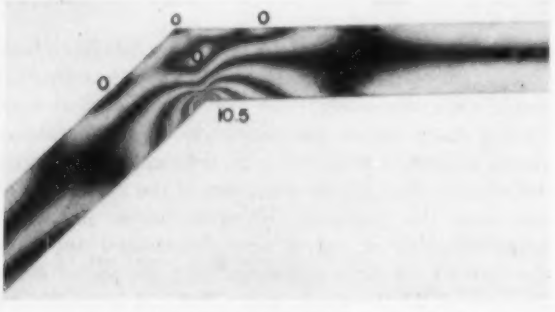

Fig. 7 Axial slice of model of conical head

The development in recent years of a simple electrical strain gage that can be cemented to the surface of the vessel permitted making multiaxial measurements in many locations on the heads. The first objective was to determine whether the results obtained with the 8-in. models would be truly representative of the results previously obtained with full-size models. If so, much more extensive testing can be done in an economical way. If was found that the model tests give reliable comparisons with the full-size prototypes.* The measurements in both cases verify the (Geckeler) computation methods.

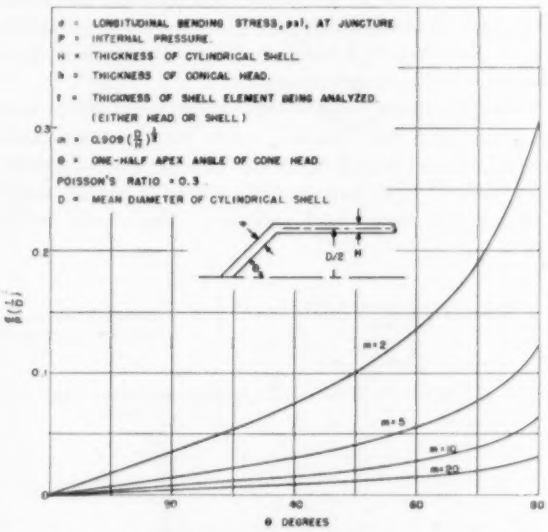

Fig. 8 Longitudinal junctional bending stress (conical pressure vessel)

The empirical methods used in designs for thin heads of the same thickness as the shell and tangent to the shell at the head-shell junction assumed that there were no longitudinal bending stresses exactly at the junction of the head and the cylinder, but that there were bending stresses of considerable magnitude on each side of this joint, which in general reached a maximum on the inside of the knuckle of the head. The careful tests indicate that there is some bending stress across the junction but that it is very small when the head and cylinder are of equal thickness.

In the case of heads of usual proportions, except hemispherical, there are compressive stresses acting circumferentially around the head near its junction with the shell. This is so because the crown portion of the head pulls the knuckle portion radially inward more than the pressure pushes it radially outward.

* The prototypes referred to are the two 48-in. diam shells with conical heads on one end and torispherical or ellipsoidal heads on the other end.

The tests on a vessel having conical heads verified the presence of high stresses at the sharp juncture, but it was found that if a section of a torus of large enough radius is provided between the cone and the cylinder, these high stress concentrations can be reduced and in some cases it may be unnecessary to make any part of the toriconical head thicker than the cylinder to which it is attached.

Another conclusion verifying previous practice is that very high local stresses in ASME Code heads near the head-cylinder junctions during the first pressure application, seldom mean that the vessel will fail in service because, if the metal is ductile, localized plastic flow during the hydrostatic test to $1^1/_2$ times the design pressure sets up residual stresses opposite in sign to those induced by pressure so that, in the regions of initial stress concentration, the service stresses change sign as pressure is applied but, in general, are less than the yield point. However, a caution must be noted here. If a vessel is subjected to frequently applied and released pressure cycles, or to a rapidly pulsating pressure, of lesser amplitude, it may fail eventually at any point of stress concentration depending on the "strain range" in the particular case, particularly around inadequately reinforced openings or at improperly designed or fabricated head-cylinder connections, even though the initial failure does not occur there in a single test to destruction. Furthermore, if corrosive conditions exist, including the caustic conditions causing so-called "caustic embrittlement" or "stress-corrosion," the deterioration of the structure of the material proceeds most rapidly at the points of high stress concentration. It is therefore necessary to take into account the service for which the vessel is to be used, as well as the stress pattern, in designing a vessel that will remain safe in operation.

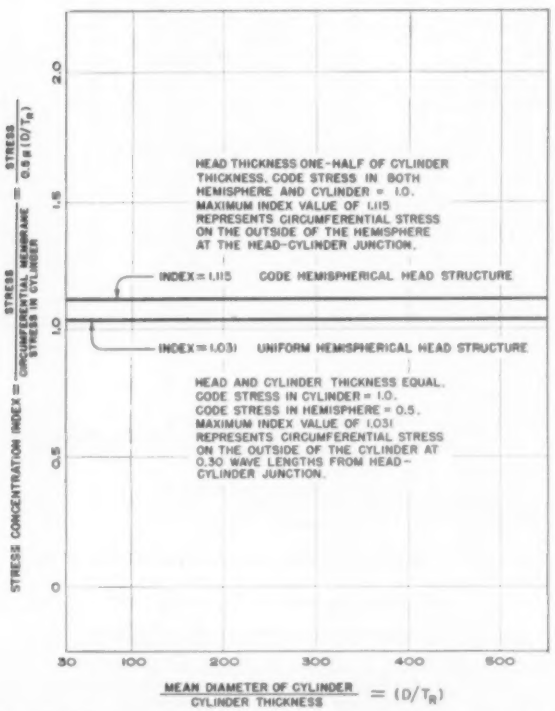
STRESS CONCENTRATION INDEX = STRESS / CIRCUMFERENTIAL MEMBRANE STRESS IN CYLINDER = STRESS / 0.5 p (D/T_R)
HEAD THICKNESS ONE-HALF OF CYLINDER THICKNESS. CODE STRESS IN BOTH HEMISPHERE AND CYLINDER = 1.0. MAXIMUM INDEX VALUE OF 1.115 REPRESENTS CIRCUMFERENTIAL STRESS ON THE OUTSIDE OF THE HEMISPHERE AT THE HEAD-CYLINDER JUNCTION.
INDEX = 1.115 CODE HEMISPHERICAL HEAD STRUCTURE
INDEX = 1.031 UNIFORM HEMISPHERICAL HEAD STRUCTURE
HEAD AND CYLINDER THICKNESS EQUAL. CODE STRESS IN CYLINDER = 1.0. CODE STRESS IN HEMISPHERE = 0.5. MAXIMUM INDEX VALUE OF 1.031 REPRESENTS CIRCUMFERENTIAL STRESS ON THE OUTSIDE OF THE CYLINDER AT 0.30 WAVE LENGTHS FROM HEAD-CYLINDER JUNCTION.
0
0.5
1.0
1.5
2.0
30
100
200
300
400
500
MEAN DIAMETER OF CYLINDER / CYLINDER THICKNESS = (D/T_R)

Fig. 9 Maximum stress concentrations in two commonly used hemispherical head structures

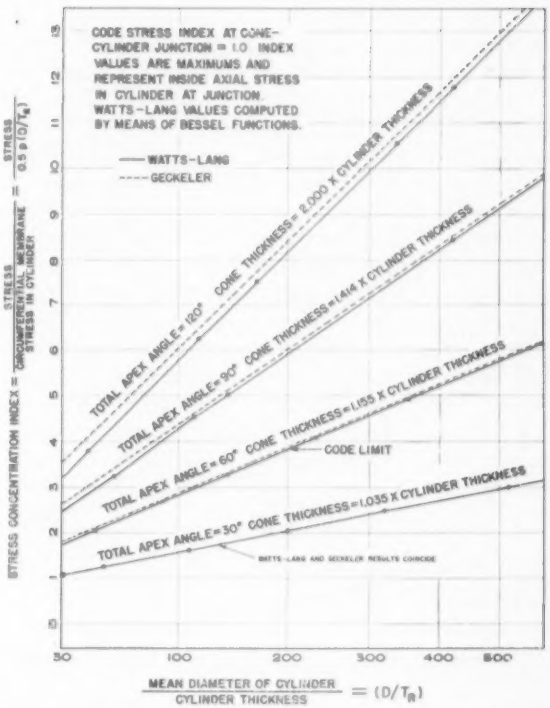
STRESS CONCENTRATION INDEX = STRESS / CIRCUMFERENTIAL MEMBRANE STRESS IN CYLINDER = STRESS / 0.5 p (D/T_R)
CODE STRESS INDEX AT CONE-CYLINDER JUNCTION = 1.0. INDEX VALUES ARE MAXIMUMS AND REPRESENT INSIDE AXIAL STRESS IN CYLINDER AT JUNCTION. WATTS-LANG VALUES COMPUTED BY MEANS OF BESSEL FUNCTIONS.
WATTS-LANG
GECKELER
TOTAL APEX ANGLE = 120° CONE THICKNESS = 2.000 X CYLINDER THICKNESS
TOTAL APEX ANGLE = 90° CONE THICKNESS = 1.414 X CYLINDER THICKNESS
TOTAL APEX ANGLE = 60° CONE THICKNESS = 1.155 X CYLINDER THICKNESS
CODE LIMIT
TOTAL APEX ANGLE = 30° CONE THICKNESS = 1.035 X CYLINDER THICKNESS
WATTS-LANG AND GECKELER RESULTS COINCIDE
1
2
3
4
5
6
7
8
9
10
11
12
13
30
100
200
300
400
500
MEAN DIAMETER OF CYLINDER / CYLINDER THICKNESS = (D/T_R)

Fig. 10 Maximum stress concentrations in four conical head structures

Previously reported elastic stresses determined through efforts of the Purdue Project Staff, have been assembled, in part, in Appendix II to present the information in a concise, simplified and easily usable form. An attempt has been made to make these summary charts entirely self-explanatory. Missing summary charts will be made available when completed.

SECTION 5—BEARING ON CODES AND DESIGN PRACTICES WITH SUGGESTIONS FOR REVISIONS

The long and generally satisfactory service record of vessels designed to the ASME and the API-ASME Pressure Vessel Codes indicates that the code rules* are safe although both analyses and tests, made on the assumption that the strains are elastic, indicate concentration indexes† for the head-cylinder zones that vary from 1.1 up to 10 or more depending on the head type and proportions. Figures 10, 11 and 12 show such indexes, and indicate that in many vessels, during the hydrostatic test, yielding occurs at localized points before the full test pressure is applied.

* "Code rules" means the ASME and the API-ASME Code rules.

† "Stress concentration index" means the ratio of the localized stress to the hoop stress in the cylinder midway between heads.

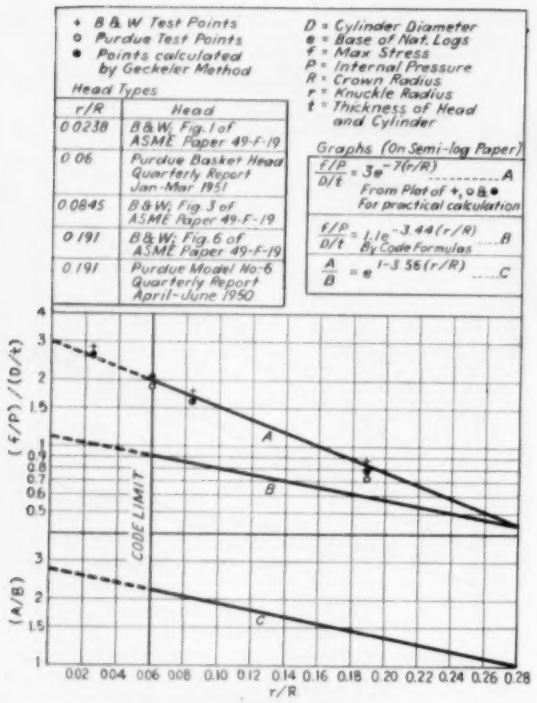
+ B & W Test Points
○ Purdue Test Points
● Points calculated by Geckeler Method
Head Types
r/R | Head
0.0238 | B&W, Fig. 1 of ASME Paper 49-F-19
0.06 | Purdue Basket Head Quarterly Report Jan.-Mar. 1951
0.0845 | B&W, Fig. 3 of ASME Paper 49-F-19
0.191 | B&W, Fig. 6 of ASME Paper 49-F-19
0.191 | Purdue Model No. 6 Quarterly Report April-June 1950
D = Cylinder Diameter
e = Base of Nat. Logs
f = Max. Stress
P = Internal Pressure
R = Crown Radius
r = Knuckle Radius
t = Thickness of Head and Cylinder
Graphs (On Semi-log Paper)
f/P / D/t = 3e^-7(r/R) ..... A
From Plot of +, ○ & ● For practical calculation
f/P / D/t = 1.1e^-3.44(r/R) ..... B
By Code Formulas
A/B = e^1-3.56(r/R) C
(f/P) / (D/t)
A/B
CODE LIMIT
A
B
C
r/R
0.02 0.04 0.06 0.08 0.10 0.12 0.14 0.16 0.18 0.20 0.22 0.24 0.26 0.28

Fig. 11 Maximum stress concentrations in torispherical heads

Figure 11 represents the results of a preliminary study of torispherical heads, which term includes those heads closely approximating ellipsoidal heads. Five cases, for which test data are available, and for all of which the crown radius differed little from the cylinder diameter, are considered in this figure. There are several solutions for the Geckeler differential equation, depending on the boundary or juncture conditions, with the properly chosen solution for a given head affording a close check with the Purdue and B & W experimental work, as can be seen from the plotted points, +, ○, and ●. To facilitate this study, it seemed desirable, however, to avoid the use of several formulas, each applying to a particular head type, and to try to get a combination of all of them. Referring to the figure, it is found that when one-half the stress index, $(f/P)/(D/t)$, for both calculated and experimental results, is plotted against r/R on semilog paper, a straight line, Graph A, results. Its equation (see "Graphs" in Fig. 11) is readily obtained, and it appears that it can be used over the range from a very shallow dished head to a 2:1 ellipsoid for practical stress calculations, provided the crown radius approximates the cylinder diameter, and the thicknesses of head and cylinder are equal. The same quantities, $(f/P)/(D/t)$, as computed by code methods, also give a straight line, Graph B, when plotted against r/R; and can be represented by an equation similar to that for Graph A. Actually the ordinates of Graph B are one-half of the corresponding "M" values of Table UA-4.2 of the 1950 ASME Code. Lastly, Graph C, or A divided by B, gives the measured and computed stress concentrations relative to the concentrations indicated by the Codes, as shown by this method.

Figure 12 represents a preliminary attempt to make a dimensional analysis study to consolidate all factors influencing the stress values in the various head types considered. This chart represents a comparison between the maximum elastic stresses in or near the juncture of head to shell, or knuckle to crown, as measured in actual vessels and model vessels, together with certain computed values, on the one hand; and the maximum stress values of the ASME Code requirements, on the other hand. Whenever the solid black dot, predicted by application of the ASME Code rules, falls below the curve, the indicated maximum elastic stress for a code vessel is greater than the code design stress. Whenever the point lies above the curve, the indicated maximum elastic stress is less than the code design stress. Thus, Fig. 12 indicates practically no stress concentration for code hemispherical heads. For a 2:1 approximate ellipsoidal head, formed by two radii, with crown radius 90% of the cylinder diameter, and knuckle radius 17% of the cylinder diameter, the indicated stress concentration index is approximately 1.6; for a 45-deg conical

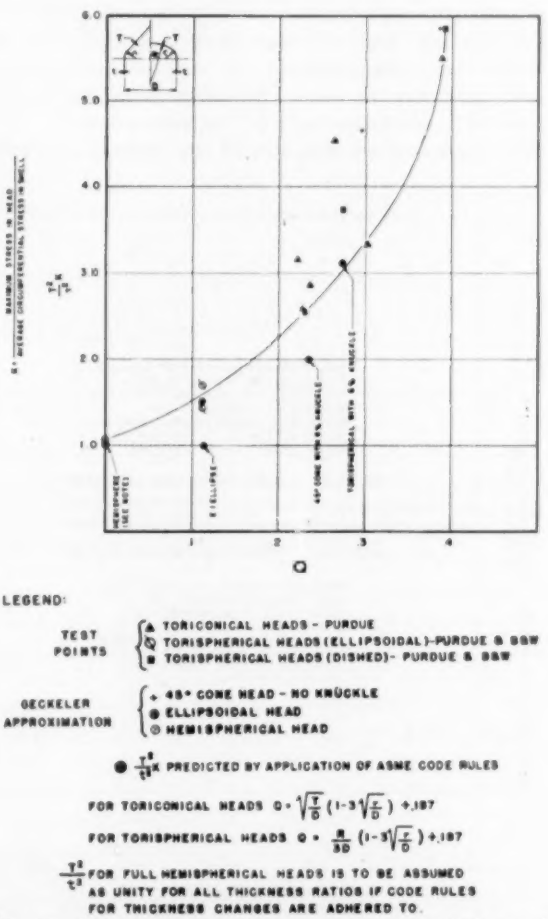
MAXIMUM STRESS IN HEAD
DIVIDED BY THEORETICAL STRESS IN SHELL
Q
LEGEND:
TEST POINTS
▲ TORICONICAL HEADS - PURDUE
Q TORISPHERICAL HEADS (ELLIPSOIDAL)-PURDUE & B&W
■ TORISPHERICAL HEADS (DISHED)- PURDUE & B&W
GECKELER APPROXIMATION
+ 45° CONE HEAD - NO KNUCKLE
⊕ ELLIPSOIDAL HEAD
⊙ HEMISPHERICAL HEAD
● PREDICTED BY APPLICATION OF ASME CODE RULES
FOR TORICONICAL HEADS Q = √(r/D) (1-3√(r/D)) +.187
FOR TORISPHERICAL HEADS Q = (R/3D) (1-3√(r/D)) +.187
FOR FULL HEMISPHERICAL HEADS IS TO BE ASSUMED AS UNITY FOR ALL THICKNESS RATIOS IF CODE RULES FOR THICKNESS CHANGES ARE ADHERED TO.

Fig. 12 Effects of shape factors on the stress indexes of pressure vessel heads

head with a 6% knuckle, the indicated stress concentration index is about 1.3; and for a torispherical head with a 6% knuckle, the indicated stress concentration index is one or less.

The currently used, comparatively simple, and partly empirical code formulas for head design are a tacit recognition by the Code Committees that the hydrostatic test pressure results in an improved stress distribution, and that localized combined stresses, exceeding the yield point at test pressure, are generally permissible, and result in vessels which are safe in service when the character of these stresses and their possible influence on failure, are properly judged. This is illustrated in Fig. 13. This recognition and open acceptance of localized stresses, higher than the stress values used in design formulas, are expressed in the following

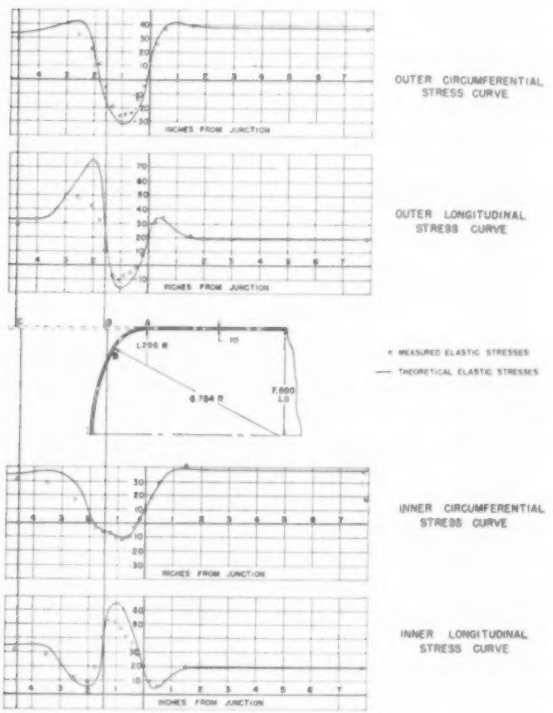

Fig. 13 Measured and computed stresses in approximate ellipsoidal head—Model 6

footnote, Par. UG-23 (a) of the 1950 ASME Unfired Pressure Vessel Code:

"It is recognized that high localized and secondary bending stresses may exist in vessels designed and fabricated in accordance with these rules. In so far as practical, design rules for details have been written to hold such stresses at a safe level consistent with experience."

If local stresses approaching the yield point are safe, it should be equally safe to streamline vessels so as practically to eliminate discontinuity stresses, and to use design stress values that permit membrane stresses greater than presently permitted. The rules now require that designs be based on one-fourth of the specified minimum ultimate strength regardless of how high the yield point may be and, as a result, steels with a high yield point cannot be used economically.

Investigations are under way to determine the conditions under which low-alloy high-yield-strength steels can be used safely and economically in pressure vessels—possibly with stress values based on yield point rather than on ultimate strength. After conclusive tests on these low-alloy steels are completed, it is the expectation of PVRC to suggest that the codes permit an option of using these steels in vessels with streamlined shape changes and details (e.g., no abrupt change in section or curvature) to be designed by code formulas, and using stress values based on yield points; provided no stress concentrations at the design pressure, computed by accepted methods which take account of the redistribution of stresses resulting from the hydrostatic test, exceed the specified minimum yield points of the steels.

Since there are only small bending moments across the junctions of ellipsoidal, hemispherical, toriconical and torispherical heads to shells of the same thickness, no head flanges (skirts) need be required in such cases, as far as stresses are concerned; they may be advisable for reasons of fabrication.

For many, but not all applications, sharp cone-to-shell junctions might be used beyond the limitations in the present ASME Code rules. It should also be practical to establish design rules for shell reducers.

The formulas of Appendix III are tentatively proposed for the design of reducers under internal pressure. They follow current Code practice in so far as the thickness of the cone or knuckle at the large diameter end is concerned; for the knuckle at the re-entrant corner, they represent the simple approach of holding the average circumferential stress in the knuckle, calculated by the general membrane formula, to the allowable stress value; or, in the case of the tension ring, designing the tension ring for the entire corner radial reaction. No doubt a complete analysis, which would include bending and discontinuity effects, would show that smaller ring sections and, in some cases, smaller re-entrant knuckle radii could be used.

SECTION 6—REMAINING WORK TO BE DONE

Heads

Although the experimental work on heads will be completed by Sept. 30, 1952, it is expected that the theoretical work will be continued by cooperating organizations and will be made available to the Design Division as it is completed. Projected theoretical studies include:

Complete hemispherical head.
Complete conical head.
Complete ellipsoidal head.
Complete hemispherical head with transition cylinder.
Disk head with central hole.

Two joined cones.
Two uniform cylinders separated by transition cylinder.
Complete basket (torispherical) head.
Theory of thick shells (D/T equals 5 to 30).
Sloped hub transition cylinder.
Hemispherical head with central hole.

As a part of this theoretical work, an effort will be made to standardize the nomenclature.

Supports and Various Types of Superimposed Loadings

The initial program contemplated by the Design Division covers a theoretical investigation of the effect of external loadings on pressure vessel shells and heads. After this is completed, an experimental program will be undertaken to check the theoretical work. It is the ultimate objective of this investigation to develop, by stress analysis and a survey of such experimental and theoretical data as may exist, reasonably accurate formulas for the influence of externally applied loadings on pressure vessels. The influence of proportions and details of simple plate clips, support lugs of various shapes, and loading imposed by nozzle connections is to be considered.

It is expected that critical stresses, both in the circumferential and longitudinal directions, will be determined as functions of the radius and thickness of the shell, and the proportions of the attachments through which loadings are applied. The possibility of buckling of the vessel wall will be considered. In addition, formulas are to be developed for the radial deflection of the shell subjected to radial loads, and for the rotation of the attachment itself due to shell deflection under imposed moments. Since it is known that stresses due to external local loadings of this type are not directly additive to pressure membrane stresses, this development will include the effect of applied loadings with and without internal (or external, when so designed) pressure on the vessel.

*Reinforced Openings**

The project on Openings in Pressure Vessels is at present concerned with an investigation with plastics of the reinforcing effect of plain cylindrical nozzles welded to pressure vessels. The method is to cast small plates of Kriston, bore them out in their centers, cement Kriston nozzles to them with fillet "welds," subject the plates to unidirectional tension, and observe the stress distribution in the plates by photoelastic means. The plate, being flat, represents only a large ratio of vessel diameter to nozzle diameter. Both these dimensions being varied, a reasonably wide field of the controlling factors is being covered. (E.g., three ratios of nozzle diameter to nozzle wall thickness are being tested, with the nozzle length in each case ranging from zero to a value at which a further increment of length would have no significant effect.)

Tests are being run as rapidly as facilities and man power permit. Much data have been gathered but quantitative results are not yet ready for publication. When the present set of runs is complete, it is planned to repeat some of them with the nozzles suitably blanked off at both ends so that internal pressure can be applied. It is also planned to stress-freeze one or more of the plates with nozzles attached; to take slices along the principal axes of the plates; and to examine the sections for stress concentration effects in the region of the plate-to-nozzle junction, and for stress distribution along the nozzle wall in the nozzle axis direction.

Some experimental work on actual nozzle connections to pressure vessels has been carried on by the Babcock and Wilcox Company and published in *ASME Transactions* July 1950. This work includes studies of various shapes and amounts of nozzle reinforcement, various methods of welding such nozzles into pressure vessel shells and elliptically shaped openings. Certain designs were shown to result in maximum stresses in the shell, and others to have the maximum stresses in the nozzle proper. Designs having a maximum stress concentration as low as 1.4 appear practical.

SECTION 7—PRACTICAL VALUE OF THE WORK

Although the record of pressure vessels made under code rules, on the whole, is excellent, increased safety can result for the thicker walled vessels because of the greater certainty in applying stress computation methods. The greatest benefit, however, will accrue eventually to the purchasers and users of pressure vessels because the logical conclusion of the research program will be the removal of most of the uncertainties remaining in pressure vessel design, and make it feasible, by streamlining, to reduce the nominal safety factor, on the hoop stress values, from its present value of four on the tensile strength to a lower value, probably as a factor of the yield point of the permissible steels particularly when higher yield point steels are used. This benefit will accrue not only to purchasers of pressure vessels of special thicknesses or shapes, but also to users of ordinary vessels provided the sources of stress concentration are practically eliminated.

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Note 1: Copies of the unpublished papers are on file in the PVRC Central Office and may be examined upon request.

Note 2: It is expected that microfilm of shell and head theory papers, collected since 1930 for G. W. Watts by Messrs. Burrows, Stephens and Graves, will be available for examination at the PVRC office in New York before the end of 1952.

Appendix I—History of the Design of Heads for Pressure Vessels*

From 1915 through the 1930's an intense development in thermal cracking processes occurred in the United States. This required thousands of pressure vessels operating for the most part at pressures of 100 to 400 psi and at temperatures of 800 to 900° F. The early plants had pressure vessels constructed by riveting. Such vessels, however, could not be kept tight for petroleum, but leaked at the riveted joints, particularly around rivet heads. In many cases, attempts were made to secure tight joints by means of fillet welds along the plate edges, and by seal welding the rivet heads, first with gas welding and later with arc welding. This was not successful unless the fillet welds were made so large that the loads were actually carried by the welds rather than by the rivets. Even in these cases, however, the tightness was not always maintained for long periods because the bare electrode deposits then used were brittle, and cracks frequently developed in the welds.

When it was noted that the welds were actually carrying the loads, some attempts were made to construct vessels entirely by welding, but again the brittleness of the bare electrode welds did not permit reliable construction. A large number of vessels for low pressure and with relatively thin walls (less than 1 in. thick) were constructed by oxy-acetylene welding, but it was not then practicable to construct heavy walled vessels (2 in. and thicker) by this means.

A process of producing vessels by forge or hammer welding already had been developed in Germany. In this process the plates to be joined were lapped and heated by gas flames to the temperature of coalescence at which the pieces were joined by pressure produced by mechanical hammering. This procedure is, in principle, the same as the blacksmith's practice of welding together two white hot bars on an anvil by hammering them. A considerable number of such vessels were built in the United States in diameters up to 10 ft, and thicknesses in the main about $^3/_4$ to 1 in. with a maximum of about 2 in.

In this process there was no way to insure that the entire length of the lap was completely welded, and occasionally hammer welded vessels failed at relatively low pressures under the conventional hydrostatic tests applied to the assembled equipment and piping. This led to testing some of these vessels to destruction which brought to notice that extensive deformation took place in the then conventionally shaped heads.

In the meantime, development of flux-coated electrodes that produced ductile welds as strong as the parent metal resulted in welding superseding riveted construction entirely for extreme operating conditions. These oil refinery practices were later followed by the use of all-welded drums in steam boilers.

Seamless forged vessels came to be used quite extensively at that time for high-pressure, high-temperature service. These vessels usually ranged from 3 to 5 in. in thickness with even heavier local sections. Generally, since these vessels were machine finished, it was more practicable to streamline the shape than in the case of forge-welded, electric-welded and gas-welded vessels.

The numerous tests of welded vessels to destruction, which accompanied the proving of the various welding processes, developed weaknesses in the then conventional shape of vessel heads which theretofore had not been emphasized and, in many cases, had passed unnoticed. The heads of riveted vessels and of the first welded vessels were usually formed into a segment of a sphere with a sharply turned knuckle and flange for attaching the head to the cylindrical shell. Since the membrane stress in a sphere is only one-half the circumferential membrane stress in cylinders of the same diameter and thickness as the sphere, it was usual to make the radius of the head twice that of the cylinder. The head thickness obtained by the usual membrane stress formulas for spheres was then the same as that computed for a seamless cylinder to which the head might be attached. No special provision was made to compensate for the added axial bending stresses at the edge of the head in the knuckle section, known even at that time to be higher than the nominal

* This history is based on original information supplied by F. L. Maker and W. R. Burrows.

tensile stress value used as the basis of the design. Such stress concentrations were assumed to be taken care of by using one-fifth of the ultimate tensile strength of the material as the working stress. This is how the nominal factor of safety of five was assumed to apply in vessel design.

In making tests of welded vessels, it was noticed that at relatively low pressures, often near the design working pressure, yielding occurred in a narrow band around the inside limit of the knuckle of the vessel. Mill scale or rust, or even paint films, would first develop a series of stress lines at 45 deg to the median line at such knuckles. These stress lines would spread somewhat at extremely high pressures, but always remained most evident at the knuckles. Starting at the knuckle, the head would deform outwardly with the knuckle radius increasing along with the bulge or depth of the dished (torispherical) head. Deformation also occurred in the cylindrical shell adjacent to the line of attachment of the head. Frequently, if the head were not too thick relative to its diameter, the plastic flow reduced the stresses so that the vessel would sustain pressure several times as great as that at which yielding first became evident. The vessel might even fail elsewhere than at the point where yielding first occurred, such as in the shell by longitudinal splitting.

As a result of such experiences, some designers in the United States concluded that the existing code rules for head design did not result in heads as strong as could be obtained from a given thickness of material. From the fact that the deformed head approached that of an elliptical cross section, they concluded that the head would be stronger if originally formed with an ellipsoidal shape. They, therefore, attempted to obtain heads initially formed with elliptical cross sections. The majority of pressure vessel manufacturers were not in a position in those early days to furnish vessels with heads of elliptical cross sections because of the impracticability of obtaining a complete range of dies for pressing the heads for all of the diameters required. It was not until many years later that large pressure vessel heads having elliptical cross sections were made available without paying for specially constructed forming dies at a cost, in some cases, several times the cost of dished (torispherical) heads with small knuckle radii.

Although it was principally the need for and experiences with processing plant vessels which revealed the weaknesses of the older types of heads in the United States, this situation had previously received considerable attention in Europe, particularly in Germany and Switzerland. As early as 1899 a report by C. Bach published in *ZVDI* (*Zeitschrift des Vereins Deutscher Ingenieure*) in Germany showed the nature of the deformations of the then conventional flanged and dished (torispherical) heads. Bach afterward (1902) reported on a number of tests on these dished heads for cases where the pressure was applied to the convex side of the head.

Theoretical investigations were being carried on in Europe prior to and concurrently with this testing of pressure vessel heads and shells. In 1893 A. E. H. Love in England published his approximate theory of thin shells, and this dissertation was brought to the attention of European engineers in 1907 through Timpe's translation of Love's work into German. In 1908, H. Reissner published a paper on "The Stress Distribution in the Walls of Cylindrical Containers" and in a later paper in 1912 he discussed "Stresses in Spherical Shells (Domes)."

In 1913, Meissner at Zürich, Switzerland, published a paper on "Elasticity Problems in Thin Shells of Annular, Spherical, or Conical Shape" based on Love's approximate theory of thin shells. Meissner reduced Love's basic equations to two linear differential equations: a first-order equation, which usually can be integrated with ease, and a fourth-order equation. The latter unfortunately cannot in general be solved readily. Meissner showed, however, that under certain conditions this fourth-order differential equation can be factored into two equations of the second order which can be solved, usually with difficulty, for most pressure vessel heads commonly used. Due to Meissner's extensive contribution, his work has become known as the Love-Meissner solution (it was referred to by this name by Geckeler in 1926).

In 1914, E. Fankhauser published a derivation of stresses, and a report of tests on heads of dished, spherical, and conical shape. These were of cast iron, some with manholes in the center and some with reinforcing rings at the edges. Curves showing the manner in which the stresses varied near the edges were included.

In 1915 Meissner published a much more extensive exposition along the same lines as his 1913 paper, with minor additions.

In their doctoral dissertations, four of Meissner's students extended his work to cover specific shell and head shapes more fully as follows: "Computation of the Strength of Spherical Shells," by Bolle, 1916; "Computation of the Strength of Knuckles," by Wissler, 1916; "Strength of Conical Shells," by Dubois, 1917; and "Computation of Conical Shells with Linearly Varying Wall Thickness," by Honegger, 1919.

The next development was what appears to be an empirical one by Klöpper. Presumably as the result of tests, he concluded that a dished (torispherical) head with a deeper dish and much enlarged knuckle radius would be an improvement over the sharp knuckle radius of the then usual head shape. Klöpper's work apparently parallels similar developments in the United States.

In 1917 H. Keller gave theoretical analyses of the stresses in dished (torispherical) heads with various edge conditions: simply supported, fixed against rotation, fixed against radial movement, etc. This report included curves indicating the nature of the

stresses near the edge, and showed that the bending stresses were in some cases considerably greater than the membrane or direct stresses.

C. Diegel published in 1920 a long report on tests made between 1905 and 1909 for the Julius Pintsch Co. (manufacturers of pressure vessels). This work included numerous tests and measurements of full size vessels, followed by tests of 31 models about 10 in. in diameter. As a result of these tests, heads of elliptical cross-section were patented in Germany (German patent 277,465, applied for February 1913, granted August 1914). They were not patented in the United States.

In the meantime additional tests were made in Switzerland by E. Hoehn, Chief Engineer of the Swiss Association of Steam Boiler Owners. His tests were started in 1914 to determine under what conditions satisfactory pressure vessels could be constructed by welding. The first tests were made on specimens of welded joints, and later a number of complete vessels were tested. The earlier tests were on welds made with bare electrodes. Subsequent ones were made with the "Quasi-Arc" process using a type of coated rod. The results were published in 1922 under the title "Tests of Autogenous and Electric Welded Parts of Boilers." This paper was followed in 1923 by a report entitled "Strength of Electric Welded Containers." Strain gage measurements were made in these tests. It was noted that the conventional dished (torispherical) heads showed the first evidence of yielding at the knuckles. This was followed, as pressure was further increased, by extensive deformations causing an approach toward elliptical cross sections. Accordingly, one vessel was built with a head originally of ellipsoidal shape. This vessel failed by a longitudinal split in the shell, with almost no deformation of the ellipsoidal head.

Hoehn was assisted in these tests by Dr. A. Huggenberger, his technical assistant, and the 1923 report included a fairly lengthy section entitled "Abridgement of the Theory of the Strength of Vessels." In this, Huggenberger develops the formulas for the membrane stresses in ellipsoids of revolution, and plots curves showing how these membrane stresses vary from the crown to the edge of the shell. He concluded the best cross section to be a 2 to 1 ellipse.

In 1925, Meissner published a third paper in Switzerland on the computation of the strength of high-pressure boiler drums. This includes an especially clear exposition of the occurrence and generation of discontinuity stresses at the junction of the head and shell of a pressure vessel.

In 1925, P. Pasternak published a summary of formulas on the bending stresses in cylindrical containers, particularly those of reinforced concrete. In this same year Siebel and Körber published an elaborate report in two sections on tests on the "Strains and Deformations of Dished Heads, With and Without Manways, Under Internal Pressure." These were tests of heads of various shapes in which careful measurements were made of the strains and changes in shape. These showed that heads of elliptical cross section are the best, although torispherical heads (so-called basket-handle shapes), with deep dished and large knuckle radii closely approximating ellipsoids, are also good.

Another very comprehensive report of a series of tests was also published in 1925 by Bach. This compared the pressures at which yielding first became evident on heads of three shapes, as follows:

(*a*) Heads with the old standard shape (dished, now called torispherical), that were relatively shallow, and with small knuckle radii.

(*b*) Deep-dish Klöpper (torispherical) heads, with larger knuckle radii.

(*c*) Ellipsoidal heads of 2:1 ratio.

All heads were riveted to cylindrical shells thicker than the heads. Two thicknesses of the Klöpper head were tested, and three thicknesses of the other shapes. These tests showed that the ellipsoidal heads withstood over three times as great a pressure as the previous standard head before yielding was noticeable in the knuckle. The Klöpper head was about 50% stronger than the standard shape.

An abstract summary of these tests appeared in English in *Power*, May 12, 1925, p. 731. This is apparently the first publication in the United States of the numerous tests made in Europe in the previous 25 years.

A more complete translation of a summary of Bach's tests published by him in *ZVDI*, March 21, 1925, the one that was the basis of the 1925 publication in *Power*, was included as Appendix 3 to a paper on the "Design of Dished and Flanged Heads" by A. B. Kinzel, *Mechanical Engineering*, 1927.

In 1924 and 1925, Hoehn in Switzerland continued the tests of the Swiss Association of Steam Boiler Owners, and published these in 1926 under the title "Riveting and Welding of Steam Boilers." This report included strength tests of welded joints, but was more concerned with computations and tests of riveted joints of vessels.

In 1926, Geckeler published a paper "On the Strength of Axially Symmetrical Shells" in which he and Bauersfeld proposed simplifying approximations of the solution of the already approximate Love theory of thin shells applied specifically to polar-symmetrical shells and heads. One of these approximate procedures, involving exponential-trigonometric functions, has been widely accepted in the United States, and is identical in principle with the method for shells referred to as Timoshenko's "Beam on an Elastic Foundation" method. As more becomes known of this approximate scheme, improved simplifications are being introduced with considerable success. In the same year, 1926, Pöschl with Terzaghi published a second edition of *Calculations for Pressure Vessels* which summarizes very completely all theoretical shell work produced up to that time, including that of Geckeler.

About the only American test of a vessel with dished (torispherical) heads reported as early as 1927, was one by S. W. Miller appearing in *Mechanical Engineering*, August 1926. This vessel had dished heads in accordance with the then current ASME Code, and one head had an inwardly flanged elliptical manway at its center. It was noted that yielding occurred first at the knuckle at below the design pressure, even though this was based on a rather low design stress of 9000 psi.

The papers that had the most direct influence on the form of the present ASME and API-ASME Code rules for dished heads were those of Hoehn and Huggenberger, both as annual reports of test work of the Swiss Association of Steam Boiler Owners, and as individual papers in the *Schweizerische Bauzeitung* and in *ZVDI*. In particular, the pamphlet "On the Strength of Dished Heads and Cylindrical Shells," published in June 1927, includes not only a very thorough discussion of the nature of the geometrical relations of dished spherical segment heads with knuckles, but also a summary of the important tests of Bach and Bauman, and of Siebel and Körber, as well as Hoehn's own tests. The second part of this pamphlet gives a complete discussion of the theory of strength of vessels, including an explanation of the discontinuity stresses, the stresses in ellipsoids of revolution, and the stresses at and near the junction.

Hoehn cited 344 cases that had been reported in Europe of failures by cracks in dished heads, most of which had been discovered in the course of inspections, but which in some 15 cases had caused violent ruptures. Although Huggenberger had developed a method of computing the stresses near joints of ellipsoidal heads to cylinders, no corresponding method was available for dished (torispherical) heads. Hoehn accordingly made an empirical approach to a design method. He analyzed all the tests of dished heads, both his own and others, that were available, and proposed values for determining as stress raising coefficient for the head shape. He found that the best correlation was obtained by comparing the stresses with the ratio of the knuckle radius to the crown radius of a dished head, r/R. He plotted about 20 points with r/R varying from about $2^1/_2$ to 19% and drew a curve through these points which he fitted to the following equation:

$$W = \frac{20(r/R) + 3}{20(r/R) + 1}$$

where W is the stress intensification coefficient. It will be noted that for $r/R = 0$ this gives a value of $W = 3.0$

This curve, modified to give higher values for the stress intensification coefficient for small values of r/R, and with results of some American tests added, was the basis of the W-curve in the API-ASME Code of 1934.

The first paper in the United States that extensively brought to the attention of the American engineers the work that had been done in Europe over many years, particularly that of Hoehn and Huggenberger, was the one by A. B. Kinzel in *Mechanical Engineering* for June 1927. This discusses the effect of the shape of dished heads on the stresses, and particularly the favorable results of using a large knuckle radius.

The principal effect of this paper was to cause the Boiler Code Committee to raise the minimum ratio of knuckle radius to the diameter of the shell from 3 to 6%, and to provide the same thickness for ellipsoidal heads (so-called) of 2:1 ratio as for seamless shells of the same diameter. These changes appeared in the 1930 revision of the Boiler Code.

In December 1928 an article by T. W. Greene on the "Design of Ellipsoidal Heads for Pressure Vessels" appeared in the *ASME Transactions*. This gives Huggenberger's formulas for stresses in ellipsoids, but does not mention that these were membrane stresses only, nor does it indicate the importance of the discontinuity stresses. In a discussion of Greene's paper, W. M. Coates pointed out the importance of discontinuity stresses and gave several references to the theoretical papers of Bolle, Meissner and Geckeler and to the tests by Hoehn and Huggenberger, and by Siebel and Körber.

The next, and one of the most important papers on the subject in America, was Coate's paper on "The State of Stress in Full Heads of Pressure Vessels," appearing in the *ASME Transactions* for December 1929. In this paper the importance of discontinuity stresses is emphasized, and the general methods of Geckeler, using step-by-step procedures for curved shells, is introduced by Coates as a means for approximating the results.

In July 1929, the English journal *Engineering* had a quite complete abstract in English of Hoehn's paper on the "Strength of Dished Heads," and emphasized the importance of having large knuckle radii.

When the American Petroleum Institute (API) Committee on Unfired Pressure Vessels was formed in 1930 to prepare a code for pressure vessels for the petroleum industry, the various papers on heads, referred to above, had just been reviewed and complete memoranda had been prepared by oil company engineers on the Pacific Coast on various subjects included therein. (In 1932, the rules so prepared received joint sponsorship of an API-ASME Committee, appointed in 1931, after modifications of the original draft.) Among these was one on "Dished Spherical and Elliptical Heads" prepared by Frank L. Maker, then with the Standard Oil Company of California. In this paper, the curves of Hoehn for dished heads were reproduced, and some additional data from tests in this country were added (see Fig. 14). At the same time computations of stresses in ellipsoidal heads of various depth ratios had been made by the Coates method. It appeared from comparisons of the latter with Hoehn's curves that similar empirical curves for dished heads should give higher values for the stress intensification coefficients for small values of r/R. Therefore, a

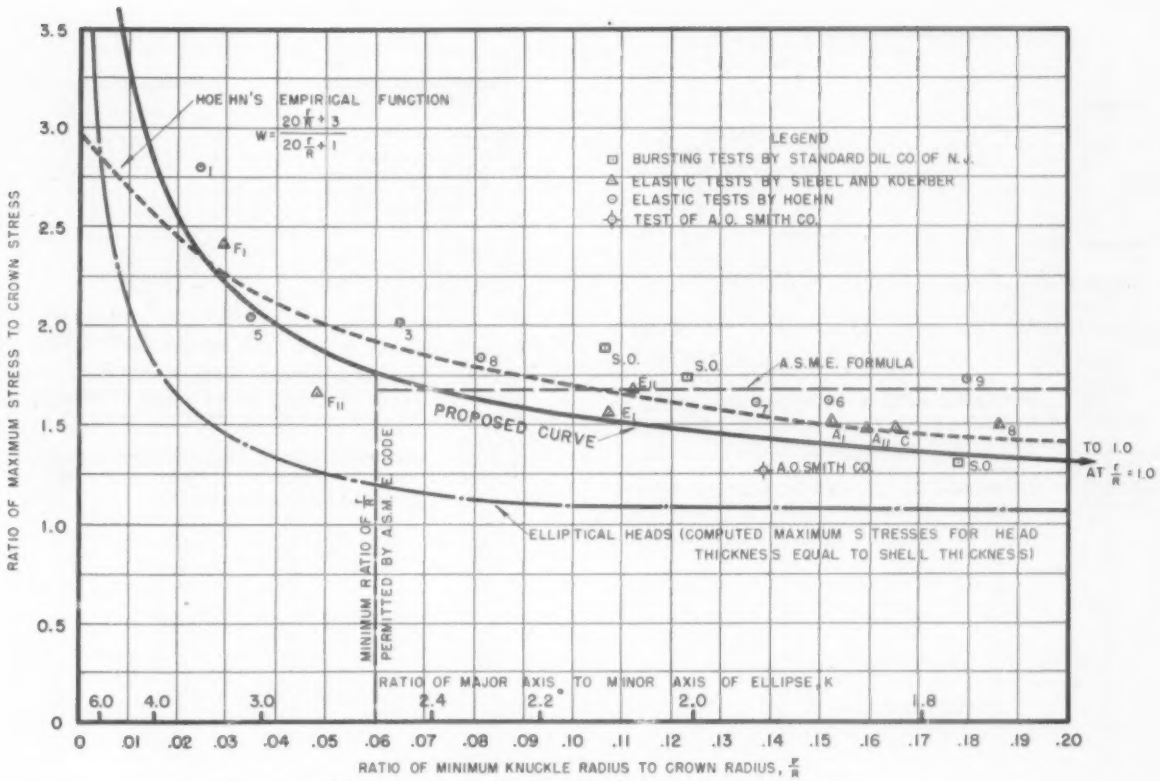

Fig. 14 Stress ratios for dished heads as a function of r/R

curve was so drawn, and was adopted by the committee as a curve for the factor W by which the thickness determined for a sphere of mean crown radius R was to be multiplied; the factor to be used in each case depending on the value of the ratio r/R of the mean knuckle to mean crown radius. The similar curve for the factor V for ellipsoidal heads was likewise included in the first edition of the API-ASME Code, published in 1934.

It will be noted from the preceding discussion that the curves (since replaced by the formulas described in Appendix IV, which are the result of work by a special committee of the ASME Boiler Code Committee) for dished (torispherical) heads were not based on computed stresses in the heads but rather on tests of heads in which the stress intensification coefficients were determined by observation of the pressure at which yielding occurred on the outside of the head at the knuckle. Actually it was known by computation that the stresses inside the knuckle were higher than those outside, but these were not readily observable. Furthermore, it was found that heads of rather poor shape initially, in which yielding was noted at fairly low pressures in tests, frequently did not fail at higher pressures at the point where yielding first occurred but, due to reduction of stress by this local plastic flow, failed elsewhere. For previous standard heads with small knuckle radii, even considerable ductility did not permit sufficient reshaping to make the head entirely satisfactory. For this reason, credit was given to the plastic deformation that occurred in the normal hydrostatic test at $1^1/_2$ times the working pressure. The Boiler Code Committee recognized the initial existence in boiler drums and headers of stress concentrations which result in computed stresses above the material's yield point under the first hydrostatic test, but which are redistributed and reduced by plastic yielding during this hydrostatic test. Dr. D. S. Jacobus prepared a letter recounting one particular situation of this sort. It related to the stresses in the ligaments of a plate having a number of tube holes. It was known that stresses around holes in plates are higher than stresses in the unpierced plate but mathematical difficulties make it impractical to compute the stresses for the more complicated cases. The Boiler Code Committee, therefore, had celluloid models made of a plate with tube holes, and the models were stressed and photographed under polarized light. These tests showed that the stress concentrations at some points were over twice the average stress values. When it came to formulate rules for the Code, the Boiler Code Committee agreed, however, to base the strength of the ligaments on the minimum cross-sectional area, and on the stress, based on this area, that had been shown to be safe in service. This resulted in accepting conditions in which portions of the ligaments would have stresses at times equal to the yield point with permanent set existing in parts of the ligaments. After the initial

TORICONICAL HEAD
STEEL MODEL NO. 1

EXPERIMENTAL WORK BY

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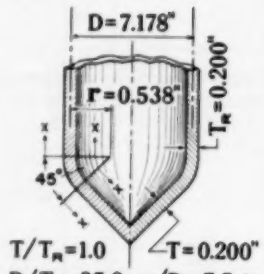

TABLE OF STRESS RATIOS (STRESS INDEXES) BASED ON EXPERIMENTALLY DETERMINED STRESSES

STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI $(0.5pD/T_R)$ IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT DISTANCE X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
STRESS RATIOS IN CYLINDER				
7.0"	—	.89	—	.94
6.0	.94	—	1.07	—
2.0	.92	.53	1.01	.91
1.0	.44	.70	.72	.96
.5	.29	.69	.46	.74
.2	.05	.12	.27	.06
0	1.66	-.45	.41	-.22
STRESS RATIOS AT CENTER OF KNUCKLE				
	2.26	-.79	.46	-.45
STRESS RATIOS IN CONE				
0	1.52	-.47	.31	-.21
.2	.75	.16	.20	.15
.5	.21	.02	.27	.55
1.0	-.06	—	.45	—

Fig. 15 Summary of test results, elastic stresses, toriconical head—Steel Model 1

TORICONICAL HEAD
STEEL VESSEL NO. A-2

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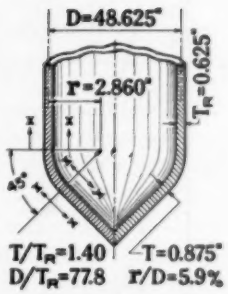

TABLE OF STRESS RATIOS (STRESS INDEXES) BASED ON EXPERIMENTALLY DETERMINED STRESSES

STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI $(0.5pD/T_R)$ IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT DISTANCE X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
STRESS RATIOS IN CYLINDER				
59.0	.51	.54	.95	1.01
5.17	.47	—	1.05	—
7.50	—	.65	—	1.04
4.67	.11	—	.72	—
4.50	—	.84	—	.90
1.17	.78	—	.90	—
1.00	—	.66	—	.91
0.0	1.11	-.36	.19	-.30
STRESS RATIOS IN KNUCKLE				
2.56	—	-.64	—	-.69
2.06	1.39	—	.55	—
1.55	—	-.97	—	-.96
1.06	1.43	—	.12	—
STRESS RATIOS IN CONE				
0.0	1.50	.59	.30	-.40
1.19	—	.10	—	.04
3.69	.56	—	.06	—
4.44	—	.97	—	.78
4.94	-.02	—	.46	—

Fig. 17 Summary of test results, elastic stresses, toriconical head— Steel Vessel A-2

TORICONICAL HEAD
STEEL VESSEL NO. A-1

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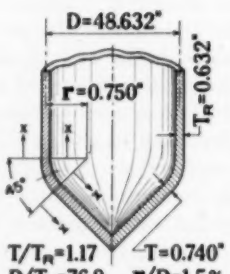

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STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI $(0.5pD/T_R)$ IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT DISTANCE X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
STRESS RATIOS IN CYLINDER				
61.105	.60	.59	1.06	1.01
10.0	.36	—	.71	—
8.5	—	.91	—	1.19
5.5	.05	.96	.65	.78
5.0	.08	.06	-.06	-.12
0.635	2.05	—	.29	—
0.0	—	-1.47	—	-1.05
STRESS RATIOS IN KNUCKLE				
	2.95	—	.39	—
STRESS RATIOS IN CONE				
.25	—	-1.51	—	-.55
.625	2.47	—	.16	—
1.00	1.95	.23	.13	-.10
5.29	.06	.87	.50	.65
7.0	-.08	1.10	.01	1.00

Fig. 16 Summary of test results, elastic stresses, toriconical head—Steel Vessel A-1

TORICONICAL HEAD
STEEL MODEL NO. 5

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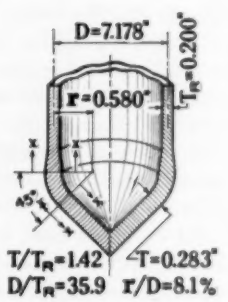

TABLE OF STRESS RATIOS (STRESS INDEXES) BASED ON EXPERIMENTALLY DETERMINED STRESSES

STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI $(0.5pD/T_R)$ IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT DISTANCE X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
STRESS RATIOS IN CYLINDER				
15.66	.46	.53	1.01	.94
6.56	.47	.46	1.06	.99
1.56	.45	.67	.99	.97
0.95	.46	.73	1.18	.93
0.91	—	.72	—	.74
0.66	.32	.62	.59	.56
0.46	—	.57	—	.35
0.36	.62	.08	.44	.14
0.33	.65	.03	.50	.14
0.18	.98	.57	.42	-.11
STRESS RATIOS IN KNUCKLE				
0.33	—	-.40	—	-.14
0.30	1.21	—	.36	—
0.17	1.27	—	.30	—
STRESS RATIOS IN CONE				
0.0	.96	-.14	.29	.01
0.17	.61	.05	.23	.10
0.50	.25	.46	.35	.35
1.00	—	.70	—	.57

Fig. 18 Summary of test results, elastic stresses, toriconical head—Steel Model 5

CONICAL HEAD
STEEL MODEL NO. 2

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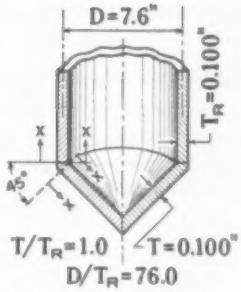

TABLE OF STRESS RATIOS (STRESS INDEXES)
BASED ON EXPERIMENTALLY DETERMINED STRESSES

STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI ($0.5p0/T_R$) IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT DISTANCE X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
	STRESS RATIOS IN CYLINDER			
6.44"	.49	.45	1.01	.99
2.47	—	.42	—	.91
2.04	.54	—	1.07	—
1.65	.41	1.03	1.06	.82
1.32	.20	.76	.97	1.09
.95	—	1.05	—	.94
.72	-.12	1.11	.48	.76
.54	.12	—	.52	—
.40	—	.69	—	.07
.28	—	.21	—	-.02
.22	.83	—	-.17	—
.10	2.57	—	-.01	—
.05	—	-1.61	—	-1.45
	STRESS RATIOS IN CONE			
.06	—	-1.52	—	-1.19
.10	2.49	—	-.01	—
.25	—	-.17	—	-.45
.31	.26	—	-.39	—
.50	.03	—	-.09	—
.54	—	1.26	—	.54
.98	—	1.43	—	1.23
1.25	.10	—	.58	—
1.50	.31	—	1.05	—
1.98	—	.41	—	.79
2.02	—	.41	—	.79

Fig. 19 Summary of test results, elastic stresses, conical head—Steel Model 2

ELLIPSOIDAL HEAD
STEEL MODEL NO. 6

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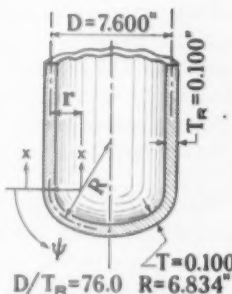

TABLE OF STRESS RATIOS (STRESS INDEXES)
BASED ON EXPERIMENTALLY DETERMINED STRESSES

STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI ($0.5p0/T_R$) IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT Distance X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
	STRESS RATIOS IN CYLINDER			
7.58"	.52	.51	.96	.97
1.50	.52	.54	1.07	1.04
.50	.15	.29	.76	.96
.25	.21	.02	.47	.67
0	.53	.53	.26	.57
Distance ψ Radians	STRESS RATIOS IN HEAD			
0	.53	.53	.26	.57
.154	.52	.57	.10	.53
.309	.96	-.05	-.10	.39
.463	1.06	-.14	-.25	.24
.617	1.23	-.19	-.33	.15
.772	1.36	-.27	-.28	.08
.956	1.39	-.18	-.16	.52
1.107*	1.06	.27	-.16	.52
1.144	.52	.89	-.09	.55
1.161	.22	1.13	.01	.53
1.255	.04	1.26	.45	.66
1.402	.72	—	.72	—
ψ/2	.86	.75	.86	.75

*Point of tangency of two component curves

Fig. 21 Summary of test results, elastic stresses, ellipsoidal head—Steel Model 6

CONICAL HEAD
STEEL MODEL NO. 3

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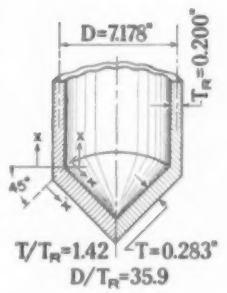

TABLE OF STRESS RATIOS (STRESS INDEXES)
BASED ON EXPERIMENTALLY DETERMINED STRESSES

STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI ($0.5p0/T_R$) IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT DISTANCE X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
	STRESS RATIOS IN CYLINDER			
15.69	.52	.57	1.07	.99
6.79	.46	—	.99	—
6.69	.54	.49	1.01	.89
1.19	.31	.05	.20	.91
0.74	.33	.70	.51	.59
0.69	.43	.68	.51	.53
0.49	.07	.31	.43	.32
0.34	—	.04	—	.05
0.24	—	-.29	—	-.20
0.09	—	-.26	—	-.35
	STRESS RATIOS IN CONE			
0.22	.04	-.25	.15	.10
0.55	.56	.40	.55	.39
0.95	.04	.05	.58	.64

Fig. 20 Summary of test results, elastic stresses, conical head—Steel Model 3

TORISPHERICAL HEAD
STEEL MODEL NO. 7

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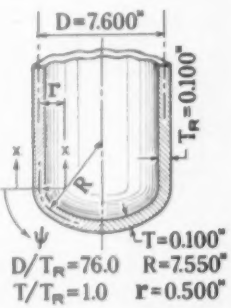

TABLE OF STRESS RATIOS (STRESS INDEXES)
BASED ON EXPERIMENTALLY DETERMINED STRESSES

STRESS RATIOS OR STRESS INDEXES ARE MEASURED STRESSES IN PSI DIVIDED BY THE CIRCUMFERENTIAL MEMBRANE STRESS IN PSI ($0.5p0/T_R$) IN THE CYLINDER OF REFERENCE

LOCATION OF STRESS MEASUREMENT Distance X	MERIDIONAL STRESS INSIDE	MERIDIONAL STRESS OUTSIDE	CIRCUMFERENTIAL STRESS INSIDE	CIRCUMFERENTIAL STRESS OUTSIDE
	STRESS RATIOS IN CYLINDER			
7.92"	.49	.40	1.01	1.02
1.50	.40	.49	1.02	.97
1.00	-.05	.52	.90	1.00
.50	-.13	1.32	.07	.60
.25	.95	.71	-.43	-.17
0	2.46	-.92	-.41	-1.29
Distance ψ Radians	STRESS RATIOS IN HEAD			
0	2.46	-.87	-.41	-1.25
.053	2.57	-1.57	-.45	-1.91
.066	5.64	-1.77	-.50	-1.88
1.068*	2.03	-.94	-.61	-1.54
1.117	.65	.22	-.75	-.54
1.170	.34	.99	-.56	.11
1.217	-.04	1.63	.08	.96
1.268	.31	1.47	.64	1.05
1.350	.76	.98	.89	.92
ψ/2	1.08	.66	1.08	.66

*Point of tangency of two component curves

Fig. 22 Summary of test results, elastic stresses, torispherical head—Steel Model 7

hydrostatic test, the normal working pressure would produce stresses less than the yield point for this case of stress concentrations of about two and a factor of safety of five or four. Experience has shown that rules so written result in vessels that are safe in service. A similar condition exists in the ligaments of riveted joints.

These described code methods and assumptions for the design of vessel heads have been very satisfactory. They have stood the test of nearly twenty years' use. In its work on heads the Design Division of PVRC is attempting to add refinement to this large store of knowledge and experience for the purpose of safely reducing below four the factor of safety as the term is generally understood in practical design procedure.

Appendix II—Summary of Test Results, Elastic Stresses, Experimental Work by Purdue University

This appendix is intended to present in a concise, simplified and easily usable form, the elastic stresses determined through the experimental test program at Purdue University (see charts in Figs. 15–22*). (Additional charts will be made available as soon as they are completed.)

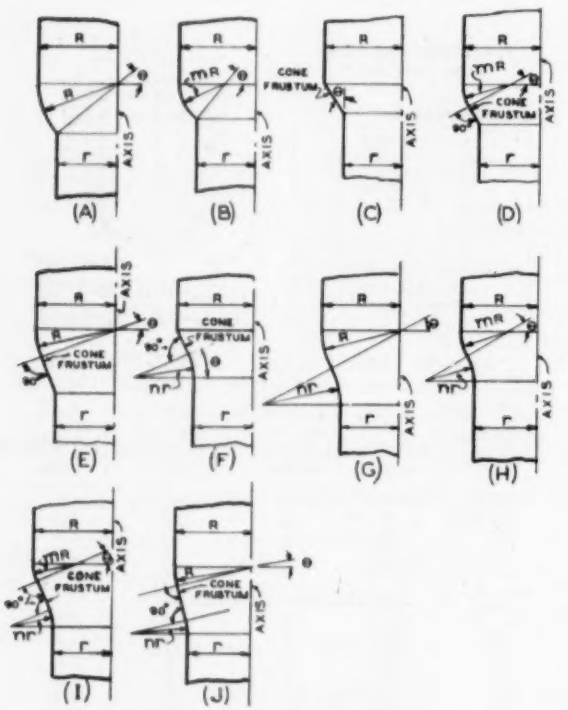

Fig. 25 Types of reducers

* Charts shown in Figs. 15–22 have been prepared by the Manufacturing Engineering Department of Standard Oil Company (Indiana).

Appendix III—Proposed Design Formulas for Shell Reducers*

The ASME Boiler Code contains no adequate rules for the design of reducers, each to connect two cylinders having a common axis but different diameters. This appendix proposes design formulas for the types of reducers shown in Fig. 23.

NOMENCLATURE

Q = $PR \div (SE - 0.6P)$, lb per in.
q = $Pr \div (SE - 0.6P)$, lb per in.
P = gas pressure, psi.
S = design stress, psi.
E = joint efficiency, as a decimal.
m and n shall be no less than 0.12.
mR and nr shall be no less than 3 times the reducer thickness, t.
t = thickness of reducer, in.

TYPE (A)

$\cos \theta = r/R$

(I) $t = {}^1/_2 Q$

(II) $T = {}^1/_2 Pr^2 \tan \theta \div SE$ = area of tension ring at junction of reducer and smaller cylinder, sq in.

TYPE (B)

$$A = \frac{R - r}{mR}; \quad \cos \theta = 1 - A; \quad \sin \theta = A \sqrt{\frac{2}{A} - 1};$$

$$Z = \frac{1 - mA}{1 - A}$$

(III) $t = {}^1/_8 QZ (3 + \sqrt{Z/m})$

(IV) $T = {}^1/_2 Pr^2 \tan \theta \div SE$ = area of tension ring at junction of reducer and smaller cylinder, sq in.

TYPE (C)

$Z = 1/\cos \theta$.
$\theta \gtrsim 30°$

(V) $t = QZ$. Furnish a compression ring at junction with larger cylinder if and as required by Par. UA-4(f).

(VI) $T = {}^1/_2 Pr^2 \tan \theta \div SE$ = area of tension ring at junction with smaller cylinder, sq in.

TYPE (D)

$$Z = \left(m + \frac{1 - m}{\cos \theta}\right).$$

(VII) $t = {}^1/_8 QZ (3 + \sqrt{Z/m})$ or QZ, whichever is the larger.

(VIII) $T = {}^1/_2 Pr^2 \tan \theta \div SE$ = area of tension ring at junction with smaller cylinder, sq in.

TYPE (E)

(IX) $t = Q$.

(X) $T = {}^1/_2 Pr^2 \tan \theta \div SE$ = area of tension ring at junction with smaller cylinder, sq in.

* Prepared by H. C. Boardman.

TYPE (F)

$Z = 1/\cos\theta$

$\theta \gtrless 30°$

(XI) $t = QZ$

or

(XII) $$t = \frac{q}{2}\left[\frac{(1+n)^2}{n}\left(\frac{\tan\theta}{\theta}\right) - n\right]$$

(Radians)

whichever is the larger.

TYPE (G)

(XIII) $$t = \frac{q}{2}\left[\frac{(1+n)^2}{n}\left(\frac{\tan\theta}{\theta}\right) - n\right].$$

TYPE (H)

$$A = \frac{R-r}{mR+nr}; \cos\theta = 1 - A; \sin\theta = A\sqrt{\left(\frac{2}{A}\right) - 1}.$$

$$Z = \left(\frac{1-mA}{1-A}\right) = \left(\frac{1-m}{\cos\theta}\right) + m;$$

$$z = \left(\frac{1+nA}{1-A}\right) = \left(\frac{1+n}{\cos\theta}\right) - n$$

$$ZR = zr; \quad \frac{Z}{z} = \frac{1-mA}{1+nA}.$$

(XIV) $t = {}^1/_8 QZ(3 + \sqrt{Z/m})$

or

(XV) $$t = \frac{q}{2}\left[\frac{(1+n)^2}{n}\left(\frac{\tan\theta}{\theta}\right) - n\right],$$

whichever is the larger.

TYPE (I)

$$Z = \left[m + \left(\frac{1-m}{\cos\theta}\right)\right]; \quad z = \left[\left(\frac{1+n}{\cos\theta}\right) - n\right].$$

(XVI) $t = {}^1/_8 QZ(3 + \sqrt{Z/m})$

or

(XVII) $t = QZ$

or

(XVIII) $$t = \frac{q}{2}\left[\frac{(1+n)^2}{n}\left(\frac{\tan\theta}{\theta}\right) - n\right],$$

whichever is the largest.

TYPE (J)

$$z = \left[\left(\frac{1+n}{\cos\theta}\right) - n\right]; \quad Z = \left[m + \left(\frac{1-m}{\cos\theta}\right)\right]$$

(XIX) $t = QZ$

or

(XX) $$t = \frac{q}{2}\left[\frac{(1+n)^2}{n}\left(\frac{\tan\theta}{\theta}\right) - n\right],$$

whichever is the larger.

Appendix IV—Analysis and Interpretation of 1950 ASME Section VIII, Rules of Pressure Vessel Head Design*

This Appendix is an analysis and interpretation of the rules of the A.S.M.E. Unfired Pressure Vessel Code pertaining to the design of pressure vessel heads. A special effort is made to bring out clearly the fact that, according to the rules, the thickness of the thinnest torispherical head of a given diameter and depth and the thickness of the ellipsoidal head of the same diameter and depth are equal, both heads being designed for the same pressure.

ELLIPSOIDAL HEADS

Par UA-4(c) gives for the design of ellipsoidal heads the formula:

$$T = \frac{PDK}{2SE - 0.2P} \qquad (1)$$

where

$$K = \frac{1}{6}\left[2 + \left(\frac{D}{2h}\right)^2\right]$$

in which

T = head thickness, in.
P = design pressure, psi.
S = design stress, psi.
E = joint efficiency, in decimal form.
D = maximum inside diameter of the head, measured perpendicular to the vessel axis, in.
h = maximum inside depth of the head, exclusive of the flange.

Equation 1 can be written:

$$T = \frac{1}{6} Q\left(2 + \frac{1}{k^2}\right) \qquad (2)$$

where

$Q = PR/SE - 0.1P.$
$k = h/R.$
$R = {}^1/_2 D.$

TORISPHERICAL HEADS

Par UA-4(d) gives for the design of torispherical heads the formula:

$$T = \frac{PLM}{2SE - 0.2P} \qquad (3)$$

where

$M = {}^1/_4(3 + \sqrt{L/r})$.
L = inside crown radius, in.
r = inside knuckle radius, in.

Equation 3 can be written:

$$T = \frac{1}{8} Qm\left(3 + \sqrt{\frac{L}{r}}\right) = \frac{1}{8} Qm\left(3 + \sqrt{\frac{m}{n}}\right) = \frac{1}{8} Qm\,(3 + \sqrt{\omega}) \qquad (4)$$

where

$m = L/R.$
$n = r/R.$
$\omega = m/n = L/r.$

Equations 3 and 4 show that all torispherical heads having the same L and ω values are of the same thickness. This fact is illustrated in Fig. 24 wherein all heads in the quadrant, only two of which are sketched, have the same L and r values and so, according to eq 3, have the same thickness. In these cases R varies

* Prepared by H. C. Boardman.

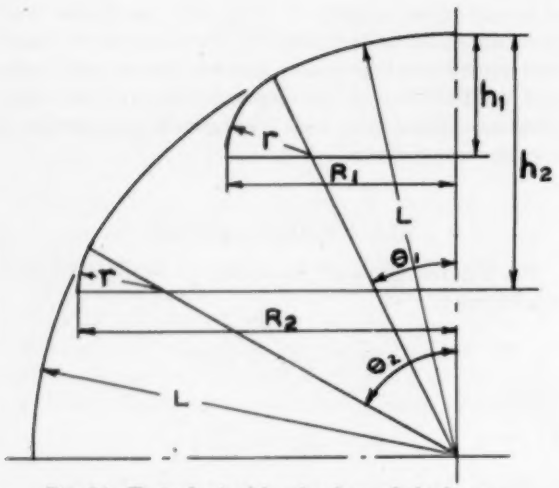

Fig. 24 Torispherical heads of equal thickness

with θ; m and n vary inversely with R; but ω is independent of θ. For $\theta = 0$ and $\theta = 90$ deg the head is hemispherical.

Equations 1 and 2 show that the thicknesses of ellipsoidal heads vary with k. Therefore there must be, for each L/r ratio, a value of θ (and k) for which the ellipsoidal and torispherical heads of the same diameter and depth have the same thickness or have thicknesses as nearly equal as possible. For $\theta = 0$, and $\theta = 90$ deg, the ratios of torispherical to ellipsoidal head thicknesses are $^1/_4\,\omega\,(3+\sqrt{\omega})$, and $^1/_4\,(3+\sqrt{\omega})$, respectively.

(It is to be noted that the above torispherical head formula is in substantial agreement with the curve of Fig. 14, Appendix I.)

COMPARISON OF ELLIPSOIDAL AND TORISPHERICAL HEADS

By means of eqs 2 and 4 the relative shapes and thicknesses of ellipsoidal and torispherical heads of the same diameters and depths can be gotten. Equation 2 is very simple, but eq 4 is quite complex although it appears simple. The complexity of eq 4 arises from

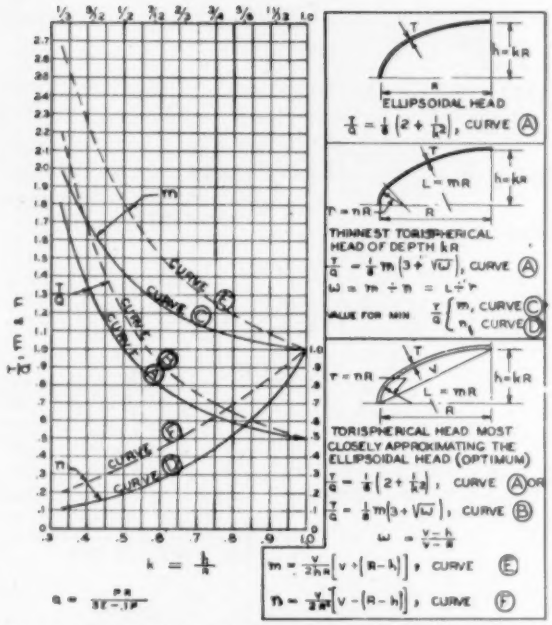

Fig. 25 Ellipsoidal and torispherical heads—Par. UA-4, 1950 ASME, Section VIII

the fact that, for each given set of R and h values, there is an infinite number of possible $L/r = m/n = \omega$ ratios for one of which the thickness is least. $L/r = m/n = \omega$ approaches infinity as L approaches infinity and as r approaches zero.

Table 2 shows for various values of $k = h/R$ and $1/k = R/h$ the ellipsoidal head thicknesses based on eq 2 and, closely, the least torispherical head thicknesses with their corresponding values of $L/R = m$ and $r/R = n$ based upon eq 4. Also listed in Table 2 are the m and n values of the torispherical heads (optimum) most closely approximating ellipsoidal heads, and their thicknesses computed from eq 4; as shown later the 1950 ASME Code permits these heads to be designed and used as ellipsoidal heads.

Table 2 and the graph in Fig. 25 show that the thinnest torispherical heads for the given k values have for all practical purposes, the *same thicknesses*

Table 2—Ellipsoidal and Torispherical Heads of Equal Thickness

$h/R = k$	$R/h = 1/k$	*Torispherical heads of least thickness* n	m	ω	T, eq 4	*Ellipsoidal heads* $j = k^2$	$c = 1/k$	T, eq 2‡	*Torispherical heads most closely approximating ellipsoidal heads (optimum)* n	m	ω	T, eq 4‡
0.339	2.95	0.12*	2.00†	$16^3/_3$	1.77Q	0.115	2.95	1.78Q	0.21	2.67	12.8	2.20Q
$^5/_{12}$	2.4	0.15	1.64	10.9	1.29Q	0.17	2.40	1.29Q	0.27	2.17	8.0	1.58Q
$^1/_3$	2.0	0.19	1.41	7.23	1.00Q	0.25	2.00	1.00Q	0.34	1.81	5.24	1.20Q
$^7/_{13}$	$^{13}/_7$	0.26	1.27	4.88	0.82Q	0.34	1.74	0.82Q	0.43	1.56	3.64	0.96Q
$^2/_3$	1.5	0.34	1.17	3.45	0.71Q	0.44	1.50	0.71Q	0.52	1.38	2.65	0.80Q
$^3/_4$	$1^1/_3$	0.47	1.11	2.38	0.63Q	0.56	1.33	0.63Q	0.63	1.25	2.00	0.69Q
$^5/_6$	1.2	0.60	1.06	1.78	0.57Q	0.69	1.20	0.57Q	0.74	1.15	1.55	0.61Q
$^{11}/_{12}$	$^{12}/_{11}$	0.73	1.02	1.39	0.53Q	0.84	1.09	0.53Q	0.87	1.07	1.23	0.55Q
1	1.0	1.00	1.00	1.00	0.50Q	1.00	1.00	0.50Q	1.00	1.00	1.00	0.50Q

* Minimum permitted (see Par. UG-32(j)).
† Maximum permitted (see Par. UG-32(j)).
‡ The 1950 ASME Code permits optimum torispherical heads to be designed by either of these columns.
jR = inside radius of curvature of ellipsoidal head at junction with its flange.
cR = inside radius of curvature of ellipsoidal head at center of head.

as the corresponding ellipsoidal heads, and that these torispherical heads lie entirely within the corresponding ellipsoidal heads, except at the flange and at the center, at each of which locations the two types of heads have a common tangent. This *"wholly within the ellipse"* relationship for torispherical heads is in harmony with long-established practice but is inconsistent with the concept that the best torispherical heads are those which in form most closely approximate the corresponding ellipsoidal heads.

In Fig. 25:

(*a*) The full *m* and *n* lines [(*C*) and (*D*), respectively] apply only to the thinnest torispherical heads.

(*b*) The full *T/Q* line, (*A*), applies both to the ellipsoidal heads and the thinnest torispherical heads.

(*c*) The dotted *m*, *n* and *T/Q* lines [(*E*), (*F*) and (*B*), respectively] apply only to the torispherical heads (optimum) most closely approximating the ellipsoidal heads.

Par. UG-81(a) of the 1950 Code reads: "The inner surface of a head shall not deviate from the specified shape by more than $1^1/_4$ per cent of the inside diameter of the head skirt (flange). Such deviations shall not be abrupt changes, shall be *outside* of the theoretical shape, and shall be measured perpendicular to the specified shape."

Figure 26 shows an ellipsoidal head and the torispherical head most closely approximating it, each head having a *k* factor of 1/2.

The optimum torispherical head is practically within the tolerance of Par UG-81(a) quoted above and, therefore, may be designed by eq 2 or by eq 4, the latter requiring a 20% greater thickness, according to Table 2 and Fig. 25, wherein *T*, for $k = {}^1/_2$, is Q, both for the torispherical head of least thickness and for the ellipsoidal head, and 1.2*Q* for the torispherical head most closely approximating the ellipsoidal head. Also shown in Fig. 26 in dotted outline, is the thinnest torispherical head, according to eq 4, Table 2 and Fig. 25.

Conical Heads

(*a*) *Heads Without a Compression Ring and Without a Knuckle.* Par UG-32(g) gives for the design of Conical heads the equation:

$$T = \frac{PD}{2 \cos \alpha (SE - 0.6P)} \qquad (5)$$

in which

T = head thickness, in.
P = design pressure, psi.
S = design stress, psi.
E = longitudinal joint efficiency, in decimal form.
D = inside diameter of the conical head, measured perpendicular to the vessel axis at the point where the thickness is to be determined, in.
α = one-half of the apex angle of the cone.

Equation 5, which is the same as the Code formula for a cylinder with an inside diameter of $D \div \cos \alpha$, is based on a circumferential stress per inch of cone

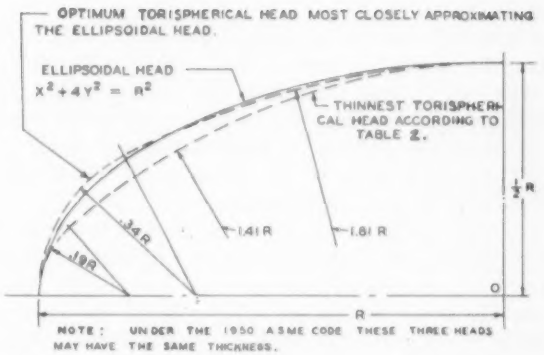

Fig. 26 2:1 ellipsoidal head and 2:1 torispherical heads of equal thickness

slant height varying directly as the distance along the slant height from the apex. That no such stress system can exist in a conical head directly joined to a cylinder is shown below.

Figure 27 represents a cross section through the conical head and containing the cone axis. The stress triangle with base $\frac{PD}{2 \cos \alpha}$ represents the total circumferential stress along the steel section from junction to apex. The length of this section is $\frac{D}{(2 \sin \alpha)}$ and the circumferential stress per inch of length, at the cylinder-cone junction, according to eq 5 is $\frac{PD}{2 \cos \alpha}$. The circumferential stress on this steel section is

$$\frac{1}{2}\left(\frac{PD}{2 \cos \alpha}\right)\left(\frac{D}{2 \sin \alpha}\right) = \frac{PD^2}{8 \sin \alpha \cos \alpha}$$

The pressure on one-half of the triangle within the conical head of Fig. 27 is

$$\frac{P}{2_i}\left(\frac{D}{2}\right)\left(\frac{D}{2 \tan \alpha}\right) = \frac{PD^2}{8 \tan \alpha} = \frac{PD^2 \cos \alpha}{8 \sin \alpha}$$

which evidently is less than the above calculated circumferential tensile stress in the steel by the amount

$$\frac{PD^2}{8 \sin \alpha \cos \alpha} - \frac{PD^2 \cos \alpha}{8 \sin \alpha} = \frac{PD^2 \tan \alpha}{8}.$$

This means that the stress system indicated by the stress triangle of Fig. 27 can be in static equilibrium only when there is either a circumferential compression

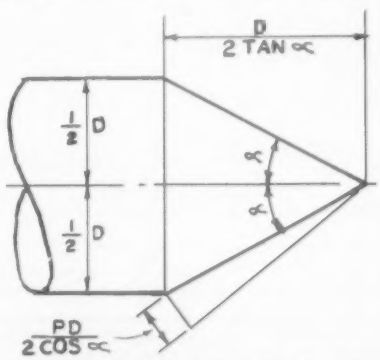

Fig. 27 Conical head directly joined to a cylindrical shell

of $(PD^2 \tan \alpha)/8$ lb at the cylinder-cone junction, or a radially outward peripheral load of $(PD \tan \alpha)/4$ lb per in. of circumference at this junction. Therefore, when, as is usual, neither of these forces exists, the cylindrical and conical shells near their junction must withstand a radially *inward* peripheral load of $(PD \tan \alpha)/4$ lb per in. of circumference which they share according to their stiffnesses.

It follows that, in fact, the portion of the conical head at and near the cylinder-cone junction is subjected to a circumferential compression and longitudinal bending stress system superimposed on the normal direct stress system implied by eq 5 and the normal discontinuity stress system.

(*b*) *Heads with a Compression Ring but Without a Knuckle.* Under some combinations of thickness, pressure, diameter and apex angle, the Code requires the installation of a compression ring at the cylinder-cone junction to prevent buckling and overstress. This is provided for in Par UG-32(h) and Par UA-4(f). Formula (7) and Table UA-4.3 of Par UA-4(f) are based on the plausible premise that the circumferential compressive stress at the junction, as calculated on the assumption of wholly elastic longitudinal strains, should not be allowed to exceed the tensile stress used in designing the cylindrical shell. The validity of this rule has never been conclusively established.

Par UG-32(g) forbids the use of conical heads directly joined to cylindrical shells when the total cone apex angle exceeds 60 degrees. This rule is arbitrary and open to question.

(*c*) *Heads with a Knuckle (Toriconical).* Par UG-32(g) requires the use of a knuckle when the total cone apex angle exceeds 60 deg; however, when a knuckle is provided, there is no limit on the cone apex angle and the knuckle thickness is calculated by the formulas of Par UA-4(d) for Torispherical heads using $D_1/2 \cos \alpha$ for the crown radius L, in which D_1 is the inside diameter at the knuckle-cone junction measured perpendicular to the cone axis.

Fatigue Tests of Steel Girders

by Hans Bühler

EARLY studies[1] of the fatigue strength of riveted and welded mild steel and low-alloy steel girders 59 in. long, 8 in. high were made on a pulsator. New tests[2] continuing this work at the Research Institute of the Vereinigten Steel Works, Dortmund, Germany, have been made on girders 16 to 36 ft long, 16 in. high using an oscillator.[3,4] It was mounted on the girder itself, Fig. 1. As the speed of rotation of the oscillator was gradually increased, the amplitude of vibration of the girder increased to a maximum, then decreased. To avoid the difficult measurement of amplitude, the power to the driving motor was recorded by a wattmeter. When the oscillator reached the critical frequency, the power used by the motor was a maximum, Fig. 2. Besides the no-load curve

Translation of "Über Dauerversuche an Stahlträgern grösserer Abmessungen," published in *Stahl u. Eisen*, **72**, 1035–1037 (1952). Translated by Dr. G. E. Claussen.

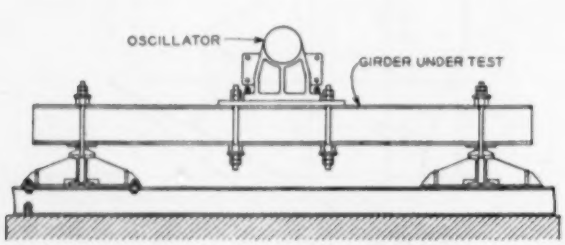

Fig. 1 Arrangements for determining the reversed stress fatigue strength of large girders

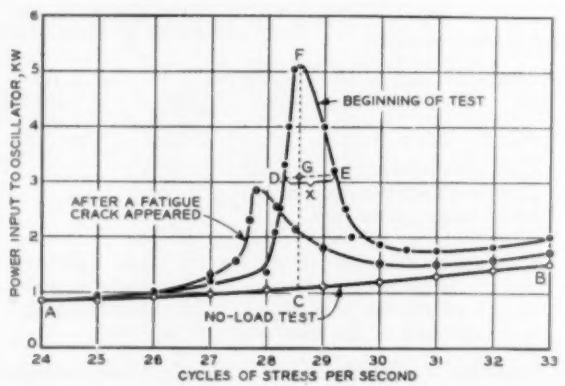

Fig. 2 Resonance curves of oscillator power on riveted girders

Table 1—Fatigue Tests on Riveted Low-Alloy Steel Girders

Girder No.	Span, ft	Natural frequency, cps	Test frequency, cps	Initial stress, psi	Measured test stresses, psi — Upper flange	Measured test stresses, psi — Lower flange	No. of cycles of stress × 10^6	Result	Location of fracture
1	26.2	9.5	9.1	2600	−20,000–+14,800	+15,900–−10,800	0.10	Broken	Upper flange
2	26.2	11.2	9.0	1800	−17,100–+13,400	+13,100–−9400	0.24	Broken	Upper and lower flange
3	13.0	32.0	30.5	600	−12,700–+11,500	+9500–−8300	1.01	Broken	Upper flange
4	14.5	29.5	29.2	600	−11,400–+10,200	+8400–−7200	1.63	Broken	Upper flange
5	14.5	28.5	27.5	600	−8800–+7700	+6500–−5400	2.04	Not broken	.....
6	16.4	24.5	24.0	1000	−11,700–+9600	Not measured	0.80	Broken	Upper flange

in Fig. 2, there are two resonance curves corresponding to the beginning of the test and to the occurrence of fatigue fracture. The resonance curve at the beginning shows a natural frequency of 28.6 cps with a power consumption corresponding to the distance *FC*. This oscillator permits measurements of natural frequency to an accuracy of 0.1 cps. The logarithmic decrement of damping is calculated from the distance *DE*, which in turn is drawn through the midpoint of the vertical line from the peak frequency to the no-load line. The logarithmic decrement is $L = \pi x/n$, where n is the natural frequency of vibration in cycles per second.

The rising part of the resonance curve is used for testing to secure most uniform regulation. In contrast to the pulsator tests, which were limited to stress ranges from zero to maximum, the stress cycle with the oscillator can be adjusted from maximum compression to maximum tension. The following girders were tested:

(*a*) Riveted girders[5] of low-alloy steel St52 with rivets of St44.

(*b*) Riveted girder butts[5] of low-alloy steel St52 with rivets of St44.

(*c*) Welded nose profile girders of mild steel St37 and low-alloy steel St52.[5]

(*d*) Welded plate girders of mild steel St37 and low-alloy steel St52.[5]

(*e*) Rolled wide flange girders (Profile 32) of St37 with and without welded flange reinforcing plates.

The results for all riveted girders[5] are summarized in Table 1. The fatigue cracks usually started at rivet holes in the flange containing the rivets that joined flange to web plate. In the earlier tests[1] the cracks started at rivet holes in the web. The ratio of span to height in the early tests was so small that shear stresses were more important than bending stresses. The present tests conform more closely to practical conditions. Early in the tests made with the pulsator the rivets became loose despite the slow speed (5 cps). With oscillator at 20 to 30 cps the rivets never became loose even after more than 3 million cycles. The reverse bend fatigue strength for the six riveted St52 girders with St44 rivets averaged ±9,300 psi. The reverse bend fatigue strength of the two riveted butt joints was ±6400 psi. Even in these tests cracks started at rivet holes in the flange.

The stiffeners of the welded girders were welded to the web and only one flange, because earlier tests[6] showed unfavorable results with stiffeners welded to both flanges. The reverse bend fatigue strength of St52 nose profile girders was ±10,600 psi. Both welded plate girders of St37 had a reverse bend fatigue strength of ±8500 psi. The nose profile girder was superior to the plate girder, but the difference was not so large as in the earlier tests with the pulsator. The low-alloy steel St52 was not so much better than mild steel St37 as in the earlier tests. Perhaps the explanation lies in the different test conditions.

As expected the fatigue cracks in the rolled, mild steel, wide flange beams with welded flange reinforcing plates started at the junction of base metal with the weld. The reversed stress fatigue strength of these beams is listed in the third column of Table 3. The probable fatigue strength (0 to max. tension) is listed in the second column. These values are up to 2800 psi lower than the range of stress in reversed stress fatigue. In every instance the oscillator results for pulsating fatigue strength are lower than the pulsator results. Probably reversed stress tests are more severe than pulsating stress tests in which the steel is subjected only to varying tensile or varying compressive stress. The larger dimensions of the girders for the oscillator tests also may be a factor. The results for the riveted low-alloy steel girders are plotted in Fig. 3. The pulsator

Table 2—Fatigue Tests on Riveted Butt Joints in Low-Alloy Steel Girders

Girder No.	Span, ft	Natural frequency, cps	Test frequency, cps	Initial stress, psi	Measured test stresses, psi — Upper flange	Measured test stresses, psi — Lower flange	No. of cycles of stress × 10^6	Result	Location of fracture
7	16.4	24.0	23.5	850	−8400–+6700	+6100–−4400	1.96	Broken	Upper flange, starting at a rivet hole
8	16.4	24.0	23.5	850	−7400–+5700	+6100–−4400	2.00	Not broken	.....
8	16.4	24.0	23.75	850	−8700–+7000	.....	0.96	Broken	Upper flange, starting at a rivet hole

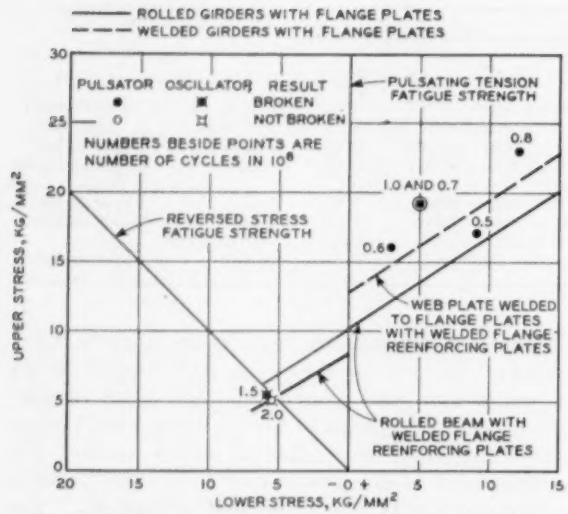

Fig. 3 Fatigue tests of riveted low-alloy steel girders

results are plotted on the right-hand side of the diagram, fracture being expected above the diagonal line. Extending the diagonal to the zero stress ordinate locates the pulsating fatigue strength as 10,000 ± 10,000 psi. Extending the diagonal still farther to the left to the reversed stress line locates the reversed stress fatigue strength as ±11,400 psi. The oscillator tests on large girders show a reversed stress fatigue strength of ±9200 psi. All girders broke that were stressed above the dotted line in Fig. 3. Extrapolating the dotted line

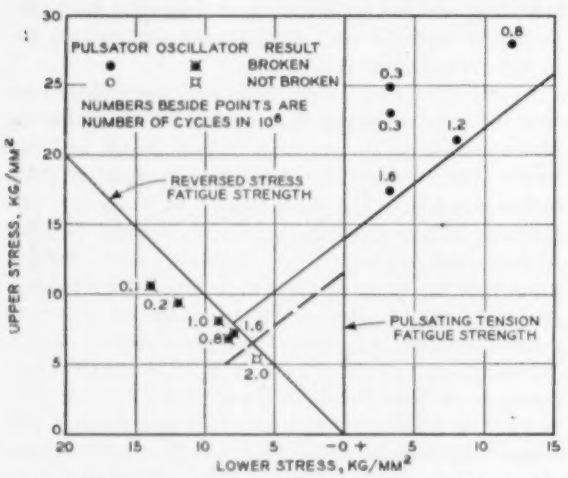

Fig. 4 Fatigue tests of mild steel girders with welded flange reenforcing plates

—— rolled girders with flange plates. – – – – welded girders with flange plates.

Table 3—Results of Oscillator Tests on I-Beams

Type of Beam	*Pulsator Tests Pulsating Fatigue Strength, Psi*	*Oscillator Tests Pulsating Fatigue Strength, Psi*	*Oscillator Tests Reversed Stress Fatigue Strength, Psi*
Riveted low-alloy steel plate girder	10,000±10,000	8,500± 8,500	± 9,200
Welded mild steel plate girder	.....	6,400± 6,400	± 8,500
Welded low-alloy steel plate girder	11,400±11,400	8,500± 8,500	±10,000
Welded low-alloy steel nose profile girder	15,600±15,600	10,000±10,000	±10,700
Rolled mild steel wide - flange beam with welded flange reinforcing plates	7,100± 7,100	5,700± 5,700	± 7,100

to the zero stress ordinate suggests a pulsating fatigue strength of 7800 ± 7800 psi to 8500 ± 8500 psi compared with the value of 10,000 ± 10,000 psi determined on the pulsator with small girders. It appears that the fatigue strength of the large girders is a little lower than the small girders.

The results of the earlier tests on welded and rolled girders with welded flange reinforcing plates, Fig. 4, suggest a pulsating fatigue strength of 8500 ± 8500 psi to 9200 ± 9200 psi for welded girders with welded reinforcement, and 7100 ± 7100 psi for rolled girders with welded reinforcement. Extrapolating the latter results leads to a reversed stress fatigue strength of about ±8500 psi for the small rolled girders with welded reinforcement. The tests on rolled mild steel wide flange girders with welded reinforcement on a span of 16.4 ft showed a reversed stress fatigue strength of about ±7100 psi and accordingly a pulsating fatigue strength of 5700 ± 5700 psi. Here again the results for the large girders are a little lower than for the small girders.

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High-Current Tungsten Arc in Argon, Helium and Their Mixtures

Measurement of voltage current characteristics and voltage gradients. Factors affecting arc stability

by Merrill Skolnik and T. B. Jones

Abstract

An experimental study was made of the properties of the high-current d-c electric arc in argon, helium and their mixtures at atmospheric pressure. Tungsten-rod electrodes were used over a current range from 10 to 100 amp. Measurements of the voltage-current and the voltage-electrode separation characteristics were made along with the plasma voltage gradients in both the pure gases and in the mixtures.

It was found that this type of arc was very stable when the system was free from oxide impurities and that the cathodic processes played a major role in the arc mechanism. The results obtained suggest that the value of the anode voltage drop for the inert gas arc over the current range studied is negligibly small. At the higher currents or with smaller diameter cathodes, tungsten was able to evaporate from the cathode in sufficient quantities to influence the character of the discharge. It was observed that the properties of the arc in mixtures of gases were in between the properties observed in the pure gases. The lower ionization potential gas, argon, had more effect on the properties of the arc in mixtures than did helium. As long as there was at least 15% argon present in the mixture, the arc properties and appearance were more like those observed in argon than in helium.

INTRODUCTION

THIS paper presents the results of an experimental study* of the high-current d-c electric arc in the inert gases argon and helium and their mixtures at atmospheric pressure. The electrodes were tungsten rods and the current range was from 10 to 100 amp. The use of inert gases and high melting-point electrodes permitted the study of the arc under controlled and reproducible conditions without being hindered by chemical reactions and excessive electrode consumption which usually accompanies the study of the arc in other gases.

Very little has been reported in the literature concerning the mechanism of this type of electrical discharge although it is not a new phenomenon and has been widely used in industry. The purpose of this investigation was simply to learn more about the fundamental properties and characteristics of the inert gas arc by carefully exploring its physical and electrical properties under controlled laboratory conditions.

Merrill Skolnik and **T. B. Jones** are connected with the Department of Electrical Engineering, The Johns Hopkins University, Baltimore, Md.

* Assisted by the ONR.
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EXPERIMENTAL EQUIPMENT

The arc was operated in a horizontal position in a cross-shaped chamber having approximate dimensions of 2 by 2 ft. A photograph of the chamber is shown in Fig. 1 (*a*), and a cross section through the plane of the electrodes is shown in Fig. 1 (*b*). This chamber was constructed from a 6-in. diam red brass pipe. The $^1/_4$-in. diam tungsten rod electrodes were arranged horizontally and placed in opposite arms of the chamber. One of the electrodes was movable to provide adjustable electrode separation. A 2-in. diam plate glass window was provided in a third arm of the chamber through which a magnified image of the arc could be projected onto a screen for visual observation. The screen was calibrated to give the electrode separation. The fourth arm contained the vacuum outlet, the gas inlet and a spring-loaded pressure relief valve. The purpose of the pressure relief valve was to limit the pressure within the chamber to 3 psi (±1 psi) above atmospheric. This was the operating pressure used in these experiments, and for all practical purposes the difference between the results at this pressure and the results at atmospheric pressure was negligible. Water-cooling tubes surrounded the chamber, and air blowers were used to limit the surface temperature. The current entered the chamber from each end through relatively large glass-to-metal lead-ins.

All measurements were made using current from a high capacity storage battery with an open circuit voltage of 100 v. The starting of the arc was accomplished by a high-voltage a-c breakdown of the gap. The electrodes and gases used were of commercial purity and were claimed by the manufacturer to be better than 99.8% pure. The gases were used as re-

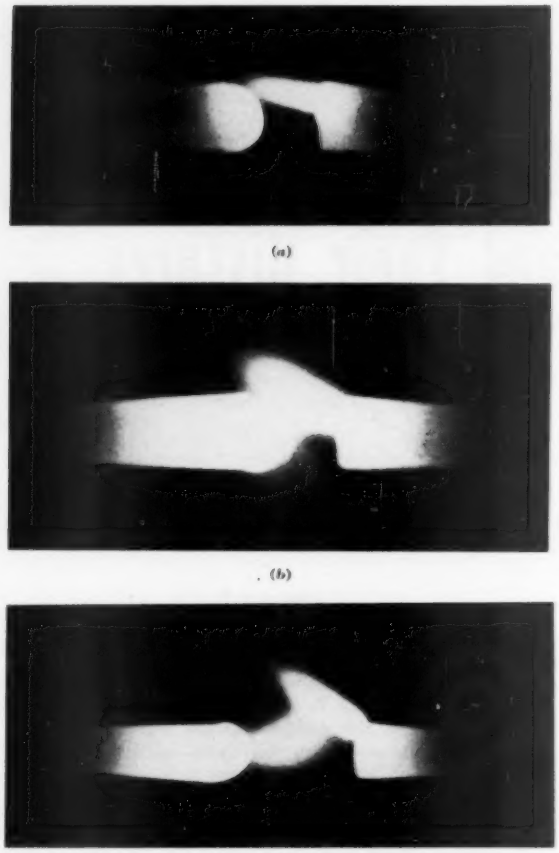
Fig. 1 (a) Photograph of the arc chamber

ceived, but the electrodes were dipped before use in hot potassium hydroxide to remove the surface oxides. The presence of surface oxides caused the behavior of the arc to be erratic and produced fluctuations in the arc voltage. The removal of the oxides by this method eliminated these undesirable effects and produced a very stable arc.

METHOD OF MAKING MEASUREMENTS

Special procedures had to be observed in filling the chamber to insure reliable measurements. The filling was accomplished by first evacuating to a pressure of 1 μ (10^{-3} mm Hg). The chamber was flushed with the gas to be studied and re-evacuated to the 1 μ level. The gas was again admitted to the chamber until the pressure was 3 psi above atmospheric, the operating pressure. The slight positive pressure in the chamber permitted gas to leak out rather than air to leak in through any minute leaks that may have been present. It was not found necessary to evacuate below a pressure of 1 μ prior to the admission of the test gas.

From the data that was obtained (voltage, current, and arc length), either voltage-current curves or volt-



(a)

(b)

(c)

Fig. 2 Appearance of the arc in argon

(a) I = 30 amp, electrode separation = 0.2 in.; (b) I = 70 amp, sep. = 0.2 in.; (c) I = 70 amp, sep. = 0.4 in. The apparent tapering of the hot electrodes is caused by the photography and is not due to thermal expansion.

age-arc length curves could be plotted. For the purpose of these measurements the electrode separation was taken to be a measure of the arc length. It is realized that the electrode separation is not always the same as the arc length, but it is a convenient approximation.

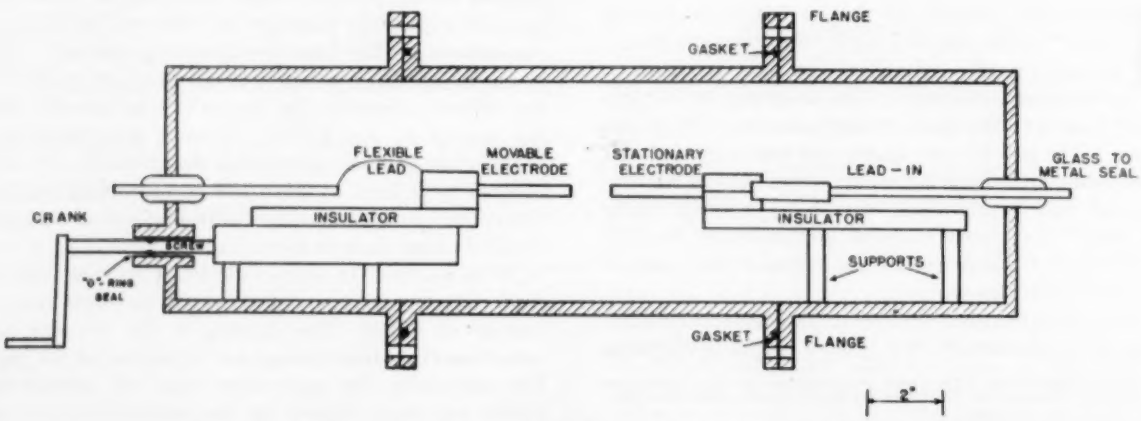


Fig. 1 (b) Cross section of the arc chamber through the plane of the electrodes

The ends of the electrodes were always flat when new. However, if the arc current were high enough to cause the anode to melt*, the surface tension of the liquid metal caused the tip of the electrode to become rounded as can be seen in Fig. 2.† The cathode, on the other hand, never became hot enough for the above to happen to it. Thus, for the sake of uniformity of the electrode appearance over the entire range of current the anode was rounded at the start of each test with new electrodes by passing a high current through the arc.

ARC APPEARANCE

The appearance of the arc in the inert gases was dependent upon the arc current and the electrode separation. In general, at close electrode separations (<0.3 in.) only the cathode flame was visible while at greater separations both the anode flame and the cathode flame appeared. With increasing current the cathode flame grew slightly in width and length and increased in intensity. When the electrode separation was increased at a constant current, the length of the cathode flame remained essentially constant, but the anode flame grew in length.

Argon

Figure 2 (*a*) is a photograph‡ of the argon-tungsten arc at a current of 30 amp and an electrode separation of 0.2 in. The anode is the rounded electrode on the left. It can be seen that the arc consists chiefly of the cathode flame which was a bluish white in color. Figure 2 (*b*) shows the same arc at a current of 70 amp. The chief effect of increasing the arc current is to increase the diameter and length of the cathode flame. In both these figures no flame is visible at the anode. This is similar to the "cathodic discharges" reported by Found[1] and by Druyvesteyn and Penning.[2] At greater separations (>0.3 in.) the anode flame is present (Fig. 2 (*c*)). At low currents this flame is wider and less intense than the cathode flame. Figure 2 (*c*) also shows how the anode and the cathode flame connect with one another. A dark space of approximately 0.04 in. can be seen separating the anode flame and the electrode. This dark space is similar to the dark space observed by Haynes[3] in short duration mercury sparks.

The configuration of the arc flames as illustrated by the three cases of Fig. 2 is termed the "normal" configuration for the flames. All measurements of the arc in argon were taken when the flames were in one of these three configurations. The flames may be above the axis of the electrodes as shown in these figures, or they may be below; there is no detectable difference in the arc characteristics. Because of thermal updraft, however, the flames were usually up.

Fig. 3 Black and white reproduction of a kodachrome negative taken with a yellow filter

The two parts of the cathode flame may be seen. $I = 35$ amp, electrode separation = 0.5 in. The anode is on the left.

Photographs taken with color filters and kodachrome film help to resolve the structure of the arc flames. In Fig. 3 is shown a black and white reproduction of a kodachrome negative taken with a yellow filter. The cathode flame is composed of two parts, an intense inner core and a much dimmer outer sheath. A small bright spot can be observed on the anode.§ This cannot be seen in Fig. 2 because the arc was too intense for it to be resolved on black and white film. If it is assumed that this bright spot represents the area over which the electrons enter the anode, then the anode current density at 35 amp is of the order of 10^4 amp/sq cm. This value is much higher than the values usually supposed for anode current densities. The spot on the anode is similar to the microspot observed by Finkelnburg with the high current carbon arc in air.[4] In his experiments the microspot wandered rapidly over the anode face causing voltage fluctuations. The spot in the inert gas arc, however, is stationary.

A qualitative estimate of the minimum value of the cathode current density may be made if the bright luminous area of the inner core of the cathode flame (as shown in Fig. 3) is taken as the area over which the electrons leave the cathode. On the basis of such an assumption the current density is 450 amp/sq cm for an arc current of 35 amp.

Helium

The characteristic shape of the flames in helium is much different than in argon, yet the basic phenomena in the two gases appear to be similar. For low currents (<30 amp) the only visible radiation from the helium arc was a flame issuing from the cathode. This is shown in the sketch Fig. 4 (*a*) and was reddish pink in color. The size of the cathode flame depended to some extent upon the current and was usually no longer than 0.3 in. nor wider than 0.2 in. (approximately). A change in arc appearance took place at a current between 40 and 50 amp. The region between the electrodes became blue and was symmetrical in shape, Fig. 4 (*b*). At higher currents the cathode flame was no longer

* In argon this current was about 85 amp while in helium it was about 70 amp. This difference in observed currents is probably due to the fact that the helium arc is a hotter arc because its arc voltage is higher for the same arc current.

† This phenomenon has also been observed with steel plates as anodes and tungsten rods as cathodes in both argon and helium under conditions simulating inert-gas arc welding. See Kouwenhoven, W. B., and Jones, T. B., *Am. Inst. Elec. Engrs.*, Misc. Paper 49-129 (April 1949).

‡ These photographs were made with a 35-mm camera at $f/16$, 1/100 sec exposure, and a red filter.

§ Because of loss of contrast in black and white reproduction, the anode spot cannot be seen distinctly in Fig. 3.

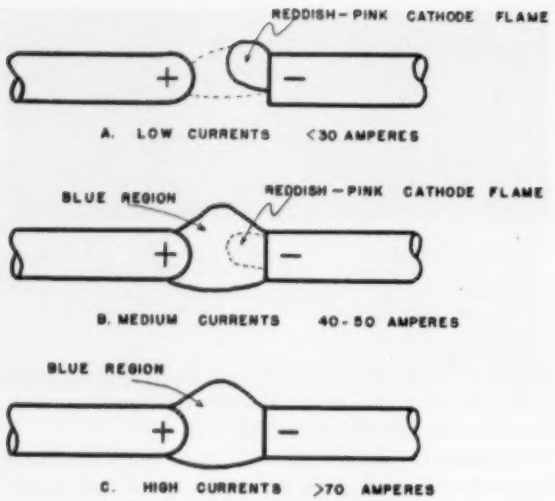


Fig. 4 Sketch of the arc appearance in helium

visible and the blue region filled the entire space between the electrodes (Fig. 4 (*c*)). The disappearance of the cathode flame may be gradual or it may be abrupt and was accompanied by a decrease of several volts in the arc voltage drop. The helium arc, just as the argon arc, was very stable provided the electrodes were free from surface oxides.

Argon-Helium Mixtures

The appearance of the arc in mixtures of these gases depended upon the relative concentrations. For mixtures ranging from pure argon to mixtures containing 85% helium, the arc had essentially the properties and appearance of the argon arc. Its cathode and anode flames were similar to the arc in argon, and these flames had the characteristic bluish-white color. There was, however, one chief difference which made the presence of helium recognizable. While the inner core of the cathode flame in argon was an intense white, this inner core with mixtures was a reddish pink, the characteristic color of the helium cathode flame. The length of the reddish-pink inner core increased with the amount of helium present. In a mixture containing 25% helium it was barely perceptible, but with a mixture containing 85% helium it had reached a length of from 0.2 to 0.3 in. Between 85 and 95% helium the arc looked more like the helium arc and less like the argon arc. Above 95% helium there was practically no resemblance to the argon arc.

In general, it may be stated that the appearance of the arc in mixtures was influenced more by small amounts of the lower ionization potential gas, argon, than by small amounts of helium.

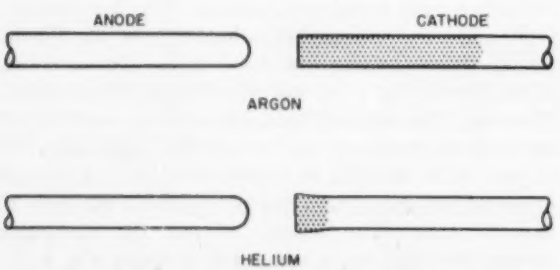


Fig. 5 Sketch of the characteristic shape of the electrodes in the two gases. The shaded areas on the cathode represent deposited tungsten transferred from the anode

Electrodes

While the arc was burning, the anode electrode appeared much hotter than the cathode electrode, except perhaps where the cathode flame attached to the cathode electrode. As can be seen from Fig. 3, the illumination from this spot was very intense. All indications were, however, that the anode was the hotter electrode. Examinations of the electrodes after a test showed that there was no appreciable loss of material from either electrode by melting. The tip of the anode was rounded and shiny while the cathode electrode was comparatively flat and appeared to be coated with a deposit of tungsten. This deposit was presumably vaporized from the hotter anode and transferred to the cathode. In Fig. 5 is illustrated the appearance of the electrodes after the tests in the two gases. In argon the deposit on the cathode extended about $2^1/_2$ in. from the face of the cathode while in helium the deposit extended only about $^3/_8$ in. but was thicker.

After the arc current had reached a value which caused the anode to liquefy and form a rounded tip, a further increase in current caused a rise in the arc voltage making the slope of the voltage-current characteristic positive. This usually occurred with currents greater than those reported here. The rate of rise, however, was small. When the slope was positive, not only was the anode molten but the melted portions could be seen to rotate. It seems very likely that the increase in arc voltage when this occurred was distributed primarily at the anode.

The appearance of the electrodes in mixtures was in between the appearance of the electrodes in the pure gases. When argon was the dominant gas (mixtures with greater than 15% argon), the cathode had a deposit of tungsten metal for some length along the electrode; while if helium was the dominant gas, the tungsten was deposited over a shorter length of the electrode.

VOLTAGE-CURRENT CHARACTERISTICS

Argon

In Fig. 6 is plotted a family of voltage-current characteristics for the argon arc at various electrode separations up to 1 in. Although the voltage as read on the voltmeter was actually the sum of the arc

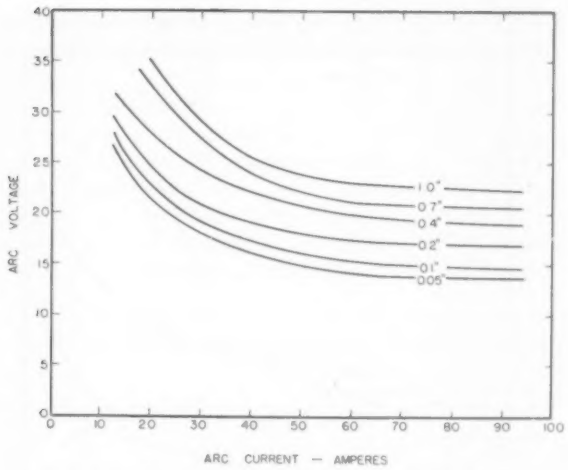


Fig. 6 Voltage-current characteristics of the arc in argon for various electrode separations

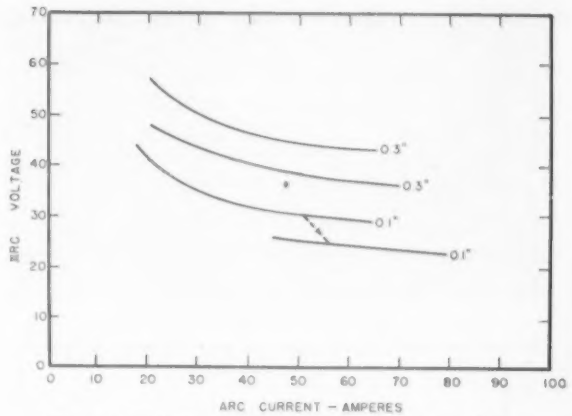


Fig. 7 (a) Voltage-current characteristics of the arc in helium for electrode separations of 0.1 and 0.3 in.

voltage plus the IR drop in the electrodes, both calculations and measurements indicated that the IR drop was small and could be neglected for the purpose of these measurements. The reproducibility of the data was good considering the nature of the measurements that were made. The curves presented here are believed to be reproducible within ±0.5 v if the arc has the same configuration as shown in Fig. 2. The curves of Fig. 6 were obtained with both increasing and decreasing current with no significant differences noted.

The voltage-current curves for the argon arc have the usual negative slope which is typical of low current d-c arcs. At the higher currents and close electrode separations the arc voltage was less than the argon ionization potential (15.5 v). This indicates that the metastable states of the argon atoms probably play an important role in the inert gas arc. The minimum arc voltage under these conditions seemed to approach close to the first excitation potential of argon (11.5 v) which is near the metastable state.

Helium

The magnitude of the arc voltage in helium was almost twice that in argon for the same arc current. In Fig. 7 (*a*) is plotted the arc voltage as a function of the arc current over the current range from 15 to 80 amp with electrode separations of 0.1 and 0.3 in. Two different curves could be obtained for each separation. Over the current range where the two curves for the same separation overlap, the arc characteristic may follow either curve. In general, however, the voltage and current will follow the upper curve for low currents and make a transition to the lower curve at high currents. This transition is indicated by the dotted line connecting the curves for the 0.1 in. separation and represents an average occurrence. The jump from the upper curve to the lower curve was random and did not occur at any definable value of current. It may be an abrupt jump or it may be a gradual change. The transition is accompanied by the appearance of the blue flame of the arc and the disappearance of the reddish-pink cathode flame as described in the previous section. For decreasing current a jump from the lower to the upper curve occurred, but the rise in voltage usually occurred at a lower current and the reverse transition was more gradual.

In Fig. 7 (*b*) is illustrated the voltage-current characteristics for separations of 0.05, 0.2 and 0.4 in. Figures 7 (*a*) and 7 (*b*) are drawn on separate coordinate axes because the curves for 0.05 and 0.1 in. separation lie very close to each other. The lower 0.4-in. curve actually lies below the upper 0.3-in. curve. Thus, in some cases it is possible to have a decrease in arc voltage as the arc length is increased at a constant current, an unusual condition for the electric arc. The curve for the 0.2 in. separation is the only curve which did not show the dual-curve feature over the current range studied. It is not understood why this did not occur.

As the current was increased at close separations the arc voltage tended to approach a value near the first

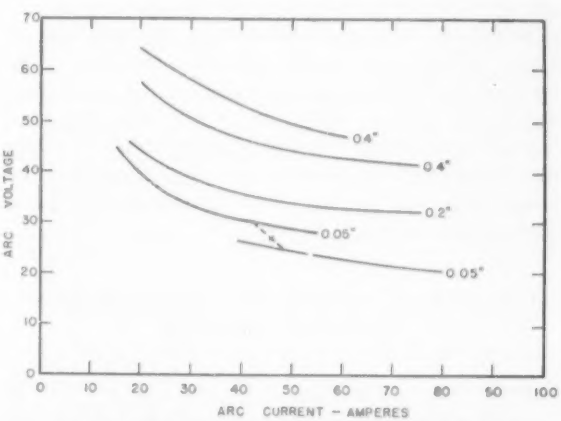


Fig. 7 (b) Same as Fig. 7 (a) but for separations of 0.05, 0.2 and 0.4 in.

excitation potential (metastable state) of helium, 19.8 v. The curves reported for helium were reproducible to within ±1 v. The dual curve feature of the helium arc will be explained in a later section.

Argon-Helium Mixtures.

Just as in the case of the arc appearance, the electrical characteristics of the arc in mixtures were a cross

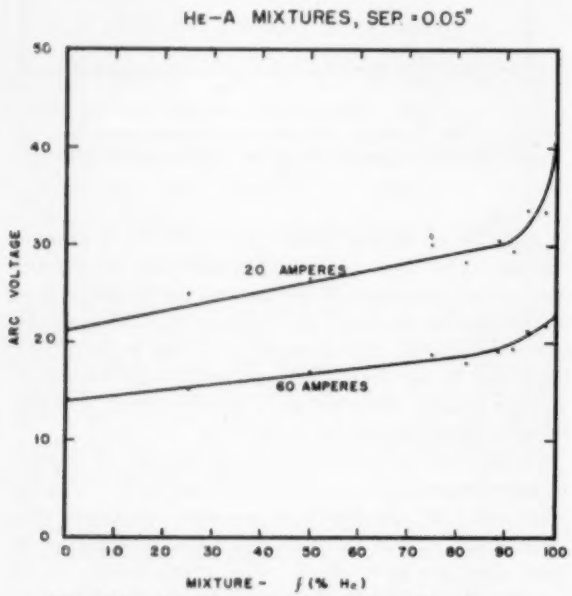


Fig. 8 (a) Arc voltage as a function of the mixture for I = 20 amp and I = 60 amp. Electrode separation = 0.05 in.

between the characteristics in the pure gases. Figure 8 (*a*) shows a plot of the arc voltage as a function of the percentage of helium present in the mixture for an electrode separation of 0.05 in. and for arc currents of 20 and 60 amp. As helium was added to pure argon, the arc voltage rose linearly until the mixture contained about 85% helium. Then the voltage rose sharply upward toward the arc voltage obtained in pure helium. The arc was strongly characteristic of argon until a sharp rise in voltage took place. The transition from the argon-dominated arc to the helium-dominated arc was continuous with increasing concentration of helium. For high concentrations of helium the dual curve feature, which was characteristic of the helium arc, was present. This is illustrated by the dotted curve in Fig. 8 (*b*).

VOLTAGE-ELECTRODE SEPARATION CHARACTERISTICS

Argon

The lower set of curves in Fig. 9 gives the arc voltage as a function of the electrode separation for the argon

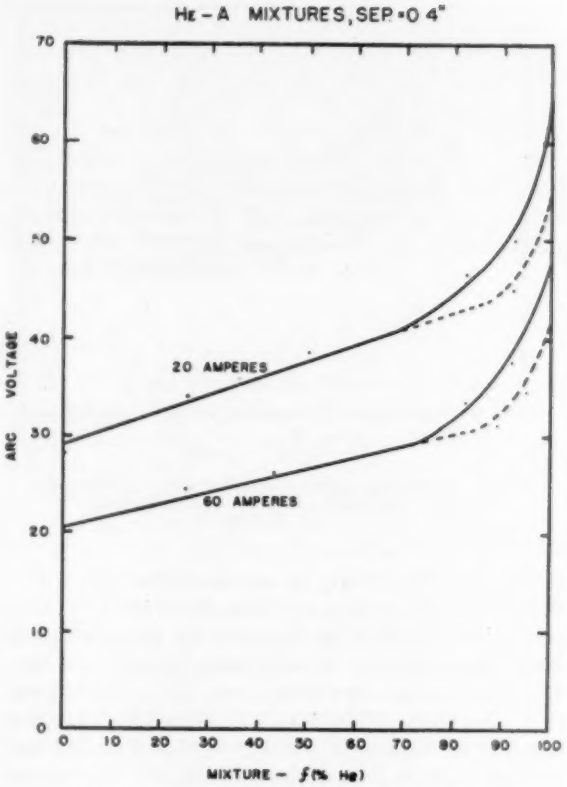


Fig. 8 (b) Same as Fig. 8 (a) but for a separation of 0.4 in.

arc at three values of current. The slope of these curves is a measure of the voltage gradient of the region between the electrodes. The usual name for this region is the *plasma*. The voltage gradient in the plasma of the high current arc at atmospheric pressure is usually supposed to be a constant function of the arc length and decreases with increasing arc current. These curves, however, show that this is not exactly the case for this type of arc. Instead, there is a separate voltage gradient associated with both the anode flame and the cathode flame. For separations between 0.05 and 0.3

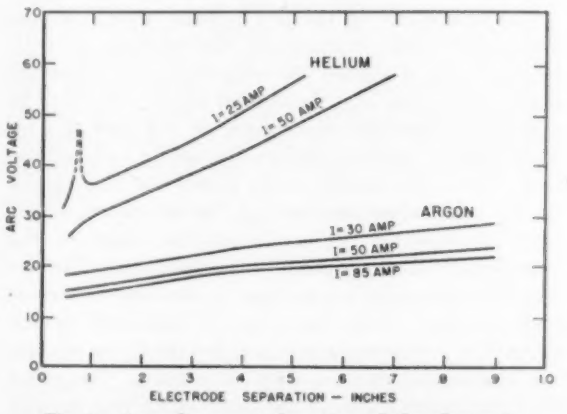


Fig. 9 Arc voltage as a function of the electrode separation

in. the plasma gradient was essentially constant over the current range studied and had an average value of 16.7 v/in. This gradient appears to be associated with the cathode flame because the anode flame was not present at these separations. The slope of this curve at the longer separations (>0.3 in.) when the anode flame was present was less than the cathode gradient and decreased with increasing current. The actual values of the gradient are given in Table 1.

These curves seem to indicate that at close separations the cathodic processes play a major role in the arc mechanism. It should be remembered, however, that the computation of the voltage gradient by this method assumes that the sum of the anode and cathode voltage drops (the voltage drops in the space charge at the electrodes) remains essentially constant for variations of the electrode separation at a constant current. This is generally thought to be a valid assumption.

Table 1—Plasma Voltage-Gradients for the Argon-Tungsten Arc

	Gradient, v/in.	
Current	*Below 0.3 in.*	*0.3–1.0 in.*
30	16.8	9.5
50	16.5	7.0
85	16.9	6.5

Helium

The voltage-electrode separation curves for the helium arc are also plotted in Fig. 9 for representative currents of 25 and 50 amp. The results are similar to those in argon and the voltage gradients are listed in Table 2. Between 0.1 and 0.3 in. the voltage gradient was essentially constant and seemed to be associated with the cathode flame. Above 0.3-in. separation the gradient had the property of decreasing with increasing current. The chief difference between these curves and similar curves in argon is that voltage gradient in helium for longer separations was greater than for shorter separations. The reverse was true in argon.

Table 2—Plasma Voltage-Gradients for the Helium-Tungsten Arc

	Gradient, v/in.	
Current	*0.1–0.3 in.*	*0.3–0.7 in.*
25	44	58
50	42	51

With currents below 30 amp in helium the voltage was found to rise 15 to 25 v over a very narrow and critical range of electrode separation. The rise is shown by the dotted portion of the 25-amp curve and it occurred somewhere between the 0.05- and 0.1-in. electrode separation. The width of the region in which this occurred was about 0.01 in. This phenomenon appeared when the cathode flame made direct contact with the anode electrode. The contact was very critical. The sharp rise in voltage was accompanied by a decrease in the size of the flame, the appearance of voltage oscillations and an audible whistling sound from the arc. Noise was also heard on a radio receiver over certain narrow frequency ranges. An oscillogram of the arc-voltage oscillations is shown in Fig. 10. These oscillations have an amplitude of approximately 12 v and a frequency between 8 and 8.5 kc. This phenomenon was not observed in arcs with currents greater than 35 amp. It has also not been observed in argon at any current employed.

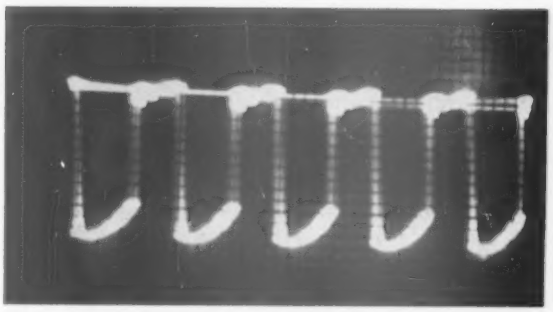


Fig. 10 Oscillogram of voltage oscillations observed in the helium arc at close separations and low currents

Argon-Helium Mixtures

The voltage-electrode separation curves for mixtures were similar to those in pure gases. In Fig. 11 is shown the voltage gradient of the cathode flame as a function of the mixture concentration at a current of 50 amp. The general shape of this curve is similar to the curves shown in Fig. 8. The voltage gradient of

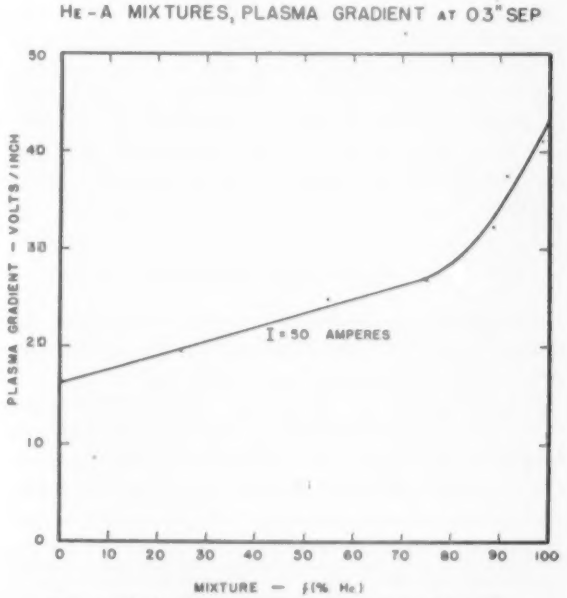


Fig. 11 Voltage gradient as a function of the mixture for I = 50 amp and for electrode separations between 0.1 and 0.3 in.

the anode flame, however, did not follow the above type of characteristic exactly. For mixtures with less than 85% helium present the anode flame voltage gradient with a current of 50 amp was essentially constant at about 7 v/in., which is approximately equal to the gradient found in the pure argon arc (see Table 1). For concentrations containing about 95% helium or more, the anode flame voltage gradient was close to that of the pure helium arc, about 50 v/in. at the same current. In the transition region between 85 and 95% helium, the arc was unstable at separations where the anode flame should have appeared and no anode flame voltage gradients could be measured. Thus it seems that the anode gradient in mixtures is either that of argon or helium, since no in between values were measured.

SMALLER DIAMETER CATHODES

Several measurements were made using 1/8-in. diam tungsten-rod cathodes and 1/4-in. diam anodes. The appearance and behavior of the arc under these conditions was quite similar to the arc where both electrodes were 1/4 in. diam. One important difference was noted, however. The arc voltage in certain cases was found to be less than the minimum excitation potential of the gas. For a separation of 0.05 in. and at the higher values of current an arc voltage of 10.6 v was found in argon and 17.5 v in helium. Both values are about 2 v lower than the excitation potential and these arcs may be classed as *low-voltage* arcs. It was also noted that the smaller diameter cathode electrode in argon appeared to have vaporized in spots, while the 1/4-in. cathode, it will be recalled, never appeared such. On the other hand, both the 1/8- and the 1/4-in. cathodes in helium appeared to have been vaporized. It is believed that the low voltage of the arc in these cases can be attributed to the presence of tungsten vapor in the cathode region. The tungsten with its lower ionization potential than either helium or argon is readily ionized and lowers the arc voltage. The result is a mixture of tungsten vapor and inert gas, and the effects are similar to those reported for gas mixtures.*

STARTING OF THE ARC

The phenomena associated with the starting of the arc are described in this section for the helium arc. Similar phenomena also occurred in argon.

A high voltage breakdown was used for the starting of the arc. A commercial arc starter, such as is used in inert-gas arc welding, was employed for placing a 60-cycle voltage of about 5000 v across the electrodes which were separated by about 0.01 in. Under proper conditions the gap would break down and the arc would follow. The process of the initiation of the arc can be divided into three steps: (1) the ionization of the gap, (2) the cold cathode arc which follows almost immediately and (3) the high-temperature arc.

In helium there was usually a time delay in the initiation of the ionization of the gap. The time interval between the instant of the application of the high voltage and the breakdown of the gap varied from near zero duration to as much as 20 sec. The longer delays occurred when the electrodes were hot, as was the case immediately after the arc had been extinguished. An average elapsed time of about 15 sec was required to allow the electrodes to cool after the arc was extinguished before the arc could be reinitiated. This was probably due to the fact that the breakdown of the gap depends to some extent on the amount of impurities present on the electrode.[5] The relatively long time interval seems necessary to allow the impurities present in the system to recondense on the cooling electrodes.

The cold cathode arc formed immediately after the ionization of the gap. This type of arc was not as intense as the high-temperature arc and was characterized by a rapidly wandering cathode spot. This arc appeared bluish in color. If the arc current was low so that the electrodes could not become sufficiently hot, the cold cathode arc would wander rapidly over the electrode and be extinguished. However, if the current was sufficiently high so that the electrodes became hot enough, the cold cathode arc would change abruptly to the very stable high temperature arc with stationary cathode spot. There was a great difference in stability between the two types of arcs. In the case of the helium arc with 1/4-in. diam electrodes, the arc current had to be above 70 amp for the high-temperature arc to appear. If the arc current was not this high, the cold cathode arc would extinguish after 1 or 2 sec. However, once the high-temperature arc was established at this high current, it could still be maintained with currents as low as 5 amp. The lowest current at which the high-temperature arc could be maintained was not established.

In argon the phenomena described above were similar except that the steps occurred faster. The breakdown of the gap and the cold cathode arc was followed by the appearance of the high-temperature arc in a fraction of a second. The minimum current necessary for the high-temperature arc to appear was much lower than in helium and was about 30 amp.

DISCUSSION

Helium Voltage-Current Characteristics

The transition that occurred in the voltage-current characteristics of Fig. 7 from the upper curve to the lower curve for increasing current and which was accompanied by a change in arc appearance seems to be caused by the presence of appreciable tungsten vapor

* This explanation of the low voltage of the arc as being due to the presence of evaporated cathode material may be applied to other cases reported in the literature. For example, see the following: Compton, Lilly and Olmstead, *Phys. Rev.*, **16**, 282 (1920). Cobine, J. D., and Gallagher, C. J., paper presented at Am. Inst. Elec. Engrs. Conference on Electric Welding, Detroit, April 1950. Gillette, R. H., and Breymeier, R. T., THE WELDING JOURNAL, **30**, Research Suppl., 146-s to 152-s (1951).

near the cathode region. This could occur when the energy at the cathode electrode becomes high enough to gennerte the required heat to evaporate sufficient material. The tungsten vapor, because it has a much lower ionization potential (8.3 v) than the minimum excitation potential of helium (19.8 v), will be ionized by the excited helium and influence the character of the discharge near the cathode to a large extent, even though the quantity present may be small. The presence of a mixture of helium and tungsten vapor near the cathode produces a lower arc voltage than with pure helium.

It may be possible that vapor from the anode caused the lower voltage. This does not seem likely, however, since examination of the electrodes shows that the lower arc voltage occurred only when the face of the cathode had a shiny appearance, indicating that appreciable material had vaporized. The appearance of the anode remained unchanged, however, and gave indication that it was always hot enough to vaporize considerable material.*

Distribution of Arc Voltage

The total voltage across the arc is made up of an anode drop, a cathode drop and the drop between these two regions, usually known as the plasma drop. At very close separations the plasma drop is so small that it may be neglected, leaving the arc voltage composed primarily of the sum of the anode and cathode drops. In the case of the inert gas arc reported here, it was found that the minimum voltage is very nearly equal to the minimum excitation potential of the gas (assuming no appreciable electrode vapor present at the cathode). It is usually supposed that the cathode drop is never less than the minimum excitation potential of the gas in which the arc is maintained.[6]

Such an assumption leads to the conclusion that in the inert gas arc the *anode drop is zero or negligibly small.* If this is the case, the major portion of the heat generated at the anode is due to the electrons giving up their "heat of condensation."† The watts input under these conditions is the product of the electron current times the work function of the anode. The electron current at the anode may be assumed equal to the arc current.

There are two indirect experimental checks that can be made to check this hypothesis:

(1) Data given by Jones and Langmuir[7] show that tungsten will reach its melting point when the power density (watts per unit surface area) is about 1200 w/sq cm. If it is assumed that the input is given by $I\phi_e$, where I is the arc current and ϕ_e the electronic work function, and that this input distributes uniformly over the face of the anode so that the power density is $I\phi_e/0.317$ (for a $^1/_4$-in. diam electrode), then the current at which the anode will melt is computed to be 85 amp. This is close to the actual observed current necessary to cause the anode to melt.

(2) The axial, steady-state temperature distribution along the electrode may be given, with some assumptions, by[8]

$$T = T_0/4\pi kx, \qquad (1)$$

where x is the axial distance from the end of the electrode at a temperature T_0; T is the temperature at the point x; k is the thermal diffusivity (for tungsten $k = 0.54$). All units are cgs.

Color photographs of the arc were obtained in which the change of color temperature along the electrode axis was quite well defined. The distance at which the color changed from white to bright orange could be easily measured. This corresponded to a temperature of 1500° K. For a 40-amp arc in argon this distance was measured as 0.32 cm. For a 40-amp arc the power input is $40 \times 4.5/0.317 = 568$ w/cm². Reference 7 shows that this corresponds to a surface temperature of 3100° K. By substituting $T_0 = 3100$ and $T = 1500$ in eq 1 the distance x is found to be 0.30 cm.

The agreement between the calculated and observed values in the above two cases is good considering the nature of the assumptions made and tends to substantiate in an indirect way that the anode drop is zero or negligible. It seems likely, however, that at higher currents when the anode face is melted, the increase in arc voltage with current mentioned previously may be due to the presence of an anode drop. It would be very interesting if this could be checked in the future by suitably designed probe measurements.

CONCLUSIONS

The inert gas arc with tungsten electrodes is one of the most stable forms of the electric arc if the system is free from oxide impurities. Its electrical properties are very dependent upon the processes occurring in the cathode flame and in the cathode drop region.

The cathode voltage drop is probably very close to the minimum excitation potential of the inert gas except perhaps when the gas is a mixture of two gases. In this case the voltage drop at the cathode is believed to lie between the minimum excitation potentials of the two gases. When appreciable electrode vapor is present near the cathode, the effect is similar to that which occurs in a mixture of two gases.

The phenomena observed at the anode over this current range can be explained by assuming that there is no observable anode drop of voltage. Consequently most of the heat generated at the anode is due to the "heat of condensation" of the electrons entering the surface of the anode. At currents higher than those reported here it is believed that the rise in voltage is due to the presence of an anode drop which appears when the surface of the anode is at the melting temperature.

* Note that considerable electrode material may be vaporized at temperatures lower than the melting point. See reference 7.

† There will be, of course, heat radiated from the plasma to the anode. For simplicity, however, this will be neglected.

The dual curve feature of the voltage-current characteristics noted for the helium arc under certain conditions is thought to be due to tungsten vapor in the cathode region. When the cathode electrode is able to evaporate sufficient vapor into the cathode region, the arc voltage will drop since the ionization potential of the tungsten is much lower than the excitation potential of the gas. This indicates that the presence of small quantities of electrode vapor can have a large influence on the properties of the arc. Certain types of low voltage arc reported in the literature may be explained on this basis.

Both the cold cathode and the high-temperature forms of the arc were observed in these experiments. The cold cathode arc appeared on starting but did not last for more than a few seconds.

Quantitative measurements were obtained of the variation of arc voltage as a function of the composition of argon-helium mixtures. It was found that the properties of the arc in mixtures were a cross between the properties observed in the pure gases. With mixtures ranging from pure argon to mixtures containing 85% helium, the properties of the argon arc were more dominant, while the influence of the helium was more pronounced for concentrations containing greater than 85% helium. In general, the effect of small amounts of argon in helium is greater than small amounts of helium in argon. However, the effect of the inert gas with the lower excitation potential on the properties of the arc is not as striking as has been reported with the low-pressure low-current discharges, such as the glow.[9]

The experiments reported in this paper represent the results of a preliminary exploratory study of the high-current inert-gas arc. Spectroscopic studies and probe measurements are indicated as the next steps that should produce results of value. The authors believe that a complete understanding of this type of arc would contribute greatly to the understanding of the general mechanism of other forms of the arc, and for this reason its continued study is planned.

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